

AN EXPERIMENTAL INVESTIGATION OF THE
AERODYNAMIC INTERACTION OF YACHT SAILS

BY

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James G. Ladesic

To Marlene, my loving wife,
for all the time and things in life
she sacrificed . . .

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KEY TO SYMBOLS

AA_n, BB_n	numerical solutions to the Glauert and image integral equations
A_{ij}	direction cosine coefficient matrix
A_n	Fourier coefficients
AR	aspect ratio
BAD	boom above deck distance
B_{ij}	general constant coefficient matrix
$C_{()}$	force or moment coefficient
C_{PF}	planform chord length
E	mainsail foot length
$F_{()}$	generalized force vector
$\bar{F}_{()}$	magnitude of the force vector components
FBA	free board area
FRF	fractional rig factor
H	height of the mast above the water plane
I	jib span
$I(z)$	flow impedance distribution function
J	fore-triangle base length
K	von Karman's constant
K_i	any generalized constant
L/D	lift to drag ratio
LOA	length overall
LP	luff perpendicular
L_t	non-dimensional turbulence scale parameter

$M_{()}$	generalized moment vector
$\overline{M}_{()}$	magnitudes of the moment vector components
OR	overlap ratio
P	mainsail span
PBL	planetary boundary layer
R_i	force balance strain gauge resistance reading
R_e	Reynolds number
SA	total planform sail area
T_i	local applied sail traction
U_A	apparent wind speed
U_B	boat speed
U_T	true wind speed
$U_T(z)$	true wind velocity profile
U_{30}	apparent/true wind speed at 30 feet
$W_{()}$	uncertainty weighting factor
x'	yacht rig parameter group
a, b	general constants
c	chord length, straight line distance from luff to leech
d	depth of draft (%)
e	exponential base
e_i	3-space unit vectors
F_i	force vector
h	reference distance from water plane to the force balance
l_H	geometric scale factor
n	reference length of the force balance

s	surface area
u^*	friction velocity
$\overline{u'v'}$	Reynolds stress
u', v'	turbulent fluctuating velocities
w	uncertainty
$w(z)$	downwash velocity
$\overline{x}_s'$	sail shape and trim parameter group
x, y, z	orthogonal coordinate system
α	angle of attack
β	apparent wind angle
γ	true wind angle
δ	sail trim angle
ϵ_{ijk}	permutation symbol
ζ_i	position vector
$\overline{\zeta}()$	magnitude of the position vector components
λ	leeway angle
Λ	sail geometry polar coordinate
ν	kinematic viscosity
ρ	density
τ	shear stress
θ	heel angle
Φ	turbulence scale correlation coefficient
ϕ	sail twist
ℓ	Prandtl mixing length

Subscripts

A,B,C,...	force balance channel indicators
F	fore-triangle
HL	heel
J	jibsail
M	mainsail
MOD	model
PF	planform
PROT	prototype
i,j,k,l...	coordinate indices or counters
x,y,z	3-space reference coordinates
mx,my,mz	moment subscripts

Abstract of Dissertation Presented to the Graduate Council
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Steady flow wind tunnel test results of sail planforms and rig configurations for typical sloops and catboats are presented. Tests were conducted in the subsonic wind tunnel at Embry-Riddle Aeronautical University, Daytona Beach, Florida, from September, 1981, through August, 1982. Force and moment data were collected using a special purpose six-component force balance. Test generated force and moment coefficient uncertainties are reported for all tests with a maximum uncertainty for lift as $\pm 4.3\%$, for drag as $\pm 6.0\%$ and for the moments as $\pm 6.1\%$. Test results are given for the entire yacht. Estimates of the forces and moments attributable to the sails alone are calculated by subtracting data of hull and rig tested without sails from the total rig data.

The one-design Finn dinghy planform was used to assess the effect of onset velocity profile distribution. A

logarithmic distribution of velocity, modeled after the planetary boundary layer (PBL) typical over water, decreased the net lift and drag coefficients approximately 28% compared with those determined for a uniform onset velocity distribution. The net sail force center with the PBL was 30% farther above the water plane than that with a uniform onset flow.

The sloop configuration was used to assess the effect of jib hoist. Results are compared to theoretically derived lift and drag coefficients. Good agreement was obtained for lift at attack angles less than 35° , the stall angle. Theoretical drag estimates are underpredicted but agree in trend. Drive force coefficients from the tests have been found to be more realistic relative to current sailing wisdom and suggest that the omission of pressure, viscous, and boundary layer interference drag in the analytical models is significant.

Further experimental research is needed to assess the effects of Reynolds number. Force center movement relative to sail trim, especially for the close reach and beat, also requires further investigation. Continued effort towards empirical stall prediction is called for. Finally, full or fractional size on-the-water tests are suggested to advance both sail and yacht design.

CHAPTER ONE

INTRODUCTION AND GENERAL EXPERIMENTAL APPROACH

Introduction

Sail design and fabrication have relied heavily on the talent of the naval architect and sailmaker (1). Effective sail production has been the result of an individual sailmaker's intuitive understanding of fluid dynamics, his knowledge of prevailing weather conditions for the locality of intended use and his adroitness in developing complicated three-dimensional surfaces from two-dimensional patterns (2). In recent times, state-of-the-art sailmaking has gone through a renaissance with the application of mathematics and digital computers to the geometric aspect of this transformation task (3). As a result, the accuracy and speed of sail shape development has been significantly improved allowing the sailmaker to market his products as "computer designed". The computer has also been applied to the more complex task of sail aerodynamics with limited success.

Computer-aided applications of lifting line and vortex lattice flow models provide a theoretical link between desired pressure distributions and the related surface geometry (4, 5, 6, 7). Recent computerized application of the vortex lattice approach by Register (7) has extended this model in two respects: it includes a logarithmic non-uniform onset velocity

patterned after that expected over open water; in addition, no linearizing assumptions are made with respect to sail shape or wake geometry. The latter is particularly significant with respect to modelling the strongly interacting sails of the sloop rig.

While application of the potential flow concept shows great promise, it must be understood that such analyses are applicable only for angles of attack less than that of the sail stall point. Superficially this encompasses the beat, close reach and beam reach headings. However even at these headings performance is determined by the ability to maintain maximum sail lift without incurring large scale separation. As such, identification of the stall point and determination of lift and drag coefficients in the vicinity of the stall point are a necessary part of a comprehensive sail study. In this range potential flow theory does not provide useful information. Furthermore, applications of lifting line and vortex lattice theories to the sail have in some instances predicted results that are contrary to accepted sailing beliefs. As a result, controversy shrouds the credibility, practicality and usefulness of predictions made by these analytical methods for sails.

Experimental Motivation

Motivation for research is usually coupled with national security and/or commercial enterprise. Admittedly, sail research has almost no application to the former and the association with the latter is on a rather small financial

scale. As such, experimental efforts have been infrequent. What has been done has often been the guarded property of the designer, naval architect or sailmaker. In the public domain one rarely finds reference to specific documentation.

The most dedicated group for altruistic experimental sail research has been that of Marchaj (8) and Tanner (9) at the University of Southampton. Much of this work has been undertaken in a large low-speed wind tunnel using fabric model sails. No apparent attempt has been made to develop an onset velocity gradient and tests have been run at Reynolds numbers based primarily on distortion and shape control rather than model similitude. The results reported are significant from a qualitative point of view. However, they have limited quantitative applicability because experimental uncertainties are unreported. Many of the tests are performed for specific yacht or planform configurations and thus are not applicable to a wide range of yachts or configurations in general.

Milgram (10, 11) has provided general, normalized, two-dimensional sail coefficients with and without mast effects at appropriate Reynolds numbers. (His experimental uncertainty is also unreported.) Methods for applying such two-dimensional sail data to a three-dimensional sail configuration for forces and moments are, at present, unclear in that no corrections for such factors as aspect ratio and sail geometry variations are provided. Milgram proposes a scheme for estimating three-dimensional pressure and viscous drag effects from two-dimensional data but admits the need for experimental

verification of the method. Such techniques do exist for air-foil sections and wings (12) but whether they apply directly to the sailboat has not been established. In short, experimental sail characteristics that can be readily employed to verify analytical results are necessary to further the advancement of sail aerodynamic research.

One fact agreed upon by both proponents and opponents of the analytical approach is the need for experimental research. Such research would not only quantify the performance of the sails tested, they would also complement the theoretical efforts by validating their range of applicability. For the sloop rigged vessel (jib and mainsail), the aerodynamic interaction associated with the close proximity of the headsail and mainsail has not been fully explored. Specifically, how performance is affected by the amount of jibsail overlap and the percentage of jib hoist has not been reported. Nor has the effect of non-uniform onset velocity. These topics are principal objectives of this research. How this experimental information compares with analytical results is also of keen interest.

Experimental Method

Measurements are made in a low-speed subsonic wind tunnel into which a flow impedance distribution can be introduced to model the nonlinear onset velocity profile at a scale compatible with the yacht model. All sails are cylindrical surfaces. Such shapes are easy to characterize geometrically, but they do not match the shapes offered by present day sailmaking

technology. Sails are constructed from aluminum sheets which are cut and rolled to shape. Once fastened to the yacht model's standing rigging, the built-in shapes are maintained throughout the tests. Yacht rig configurations tested fall into three categories: a cat rigged, Finn-type sail which is tested without a hull model in order to assess the influence that onset velocity profile has on the sail force magnitude and its location relative to the water reference plane; a variable hoist series, hull present, in which the headsail hoist is decreased at a constant main sail size; and a rig aspect ratio series, hull present, for the masthead sloop.

Tests are conducted at velocities that model a 4 to 6 kt true wind speed at the masthead. All data are corrected for wall effects, solid blocking and wake blocking. Each datum point displayed is presented with its estimated experimental uncertainty. Some salient aspects of sail testing and theoretical extension into very low Reynolds number regimes are discussed.

In the present effort, three-dimensional sails are employed to assess the effects of trim and rig under steady flow conditions. These trends are compared to the vortex lattice analysis completed by Register for identical configurations and trim and to the vortex line model developed earlier by Milgram for similar rig configurations and trim.

CHAPTER TWO EXPERIMENTAL SIMILITUDE FOR THE SAILING YACHT

General Problem of Complete Yacht Similitude

Experimental efforts in yacht testing are classically divided at the air-water interface. That is the hydrodynamics of the hull are evaluated independently of the aerodynamics of the sails. This division is convenient in that it permits a reduction in the number of parameters required for model similarity but it cannot assess interactive effects that are a consequence of the air-water interface. As such, hull hydrodynamic results must be partnered with sail aerodynamic results on the basis of assumed interface conditions. It has not been practical to attain complete experimental similitude in either sail aerodynamics or hull hydrodynamics (13, 14). It is standard practice for these methods to depart from strict compliance with similarity laws. For convenience the inference is often made that sail theory is an extension of the body of literature developed for aircraft wings. As such, differences in the governing parameters are sometimes assumed trivial and design parameters are deduced from existing airfoil data, such as that collected in Abbott and von Doenhoff (12). However, applications of such data even to the single cambered, flexible sail must be done with

caution as significant differences between the two exist. As differentiated from the rigid aircraft wing, cloth sails are normally highly loaded and generally have large geometrical twist. In addition, the criterion for optimum sail performance must be related to the projections of lift and drag to the vessel's center line that resolve into the driving force; whereas wing optimum performance is governed primarily by lift alone. Also, the sail is subject to a non-uniform onset velocity resulting from the atmospheric boundary layer; while in contrast, the aircraft wing moves through an air mass that is to the first order stationary. As a consequence of these effects, the variation of lift with attack angle cannot be considered a constant for sails, whereas this is a typical assumption in wing design. In addition, tests for aircraft are usually conducted at Reynolds numbers much larger than those encountered on yacht sails. These differences clearly justify a treatise on sail test similitude based on the pertinent variables and conditions that are unique to the sailing yacht.

For the work presented herein, the hydrodynamic effects of the hull are ignored. The scaled true wind magnitude is determined by the available wind tunnel capacity for the model size selected, with the profile impedance screen in place. This wind speed is equivalent (based on Reynolds number) to prototype on-the-water wind speed of 4 to 6 kt.

As such, heel effects for the yacht being modeled can be ignored and an assumption of no wave-air interaction is reasonable. Thus, it is not necessary to model motions that would result from a wind driven sea state.

A major discrepancy between this work and on the water conditions is that the onset flow has zero twist which, in turn, implies the assumption of zero boat speed. This limitation is a practical necessity in wind tunnel testing. To model the effect of boat speed, one could either move the model at constant speed in a direction oblique to the tunnel flow during data collection or modify the wind tunnel airflow in both direction and magnitude with the yacht model stationary. Either procedure could simulate the vectorial addition of boat speed and true wind speed. The former would require a suitably wide tunnel test section and would introduce uncertainties associated with model inertial response. The latter concept would complicate the synthesis of the onset velocity profile. For present purposes, the added complexity of either procedure is unwarranted in terms of what it would contribute to the results. Therefore, the distortion of results that are a consequence of zero boat speed are accepted. The effect of this distortion on the overall test results is assessed in Appendix A; it is shown to be small.

Finally, the apparent heading ($\beta - \lambda$) of a yacht sailing to weather is different from the true heading measured relative to the apparent wind by the angle λ . This small difference is measured from the yacht center line and is referred

to as the leeway angle. This angle is the effective angle of incidence for the keel. While λ is small (on the order of 4 degrees), it is necessary for the development of the keel reaction to the sail-produced heel force. Consequently, the desired component resolution of the aerodynamic sail forces are taken parallel and perpendicular to a plane passed vertically through the yacht and rotated λ from the yacht center line. Since the hydrodynamic properties of the hull and keel are unknown and, therefore, ignored in the present work, the aerodynamic sail forces are resolved parallel and perpendicular to the boat center line. This is equivalent to assuming $\lambda = 0$. It is seen from Figure 2-1 that this assumption will slightly decrease the component of lift and increase the component of drag in the selected driving force direction and is therefore conservative with respect to the net predicted driving force.

Sail Test Similitude

Dimensionless variables can be formulated by application of Buckingham's Pi Theorem (15). This is the approach adopted here. For the sailing yacht, forces and moments of interest are resolved with respect to the wind tunnel flow direction. Figure 2-1 illustrates the Cartesian coordinate frame selected for this purpose. From the assumption that $\lambda = 0$, the force coefficient is then defined functionally as

$$\frac{F}{\frac{1}{2}\rho U_A^2 (SA)} = C_F[\beta, \theta, \frac{U_T(z)}{U_T(z_{MAX})}, \frac{U_B}{U_A}, \frac{U_T}{U_A}, \frac{U_A^c}{v}, x', \bar{x}_S', L_t, l_H] \quad (2-1a)$$

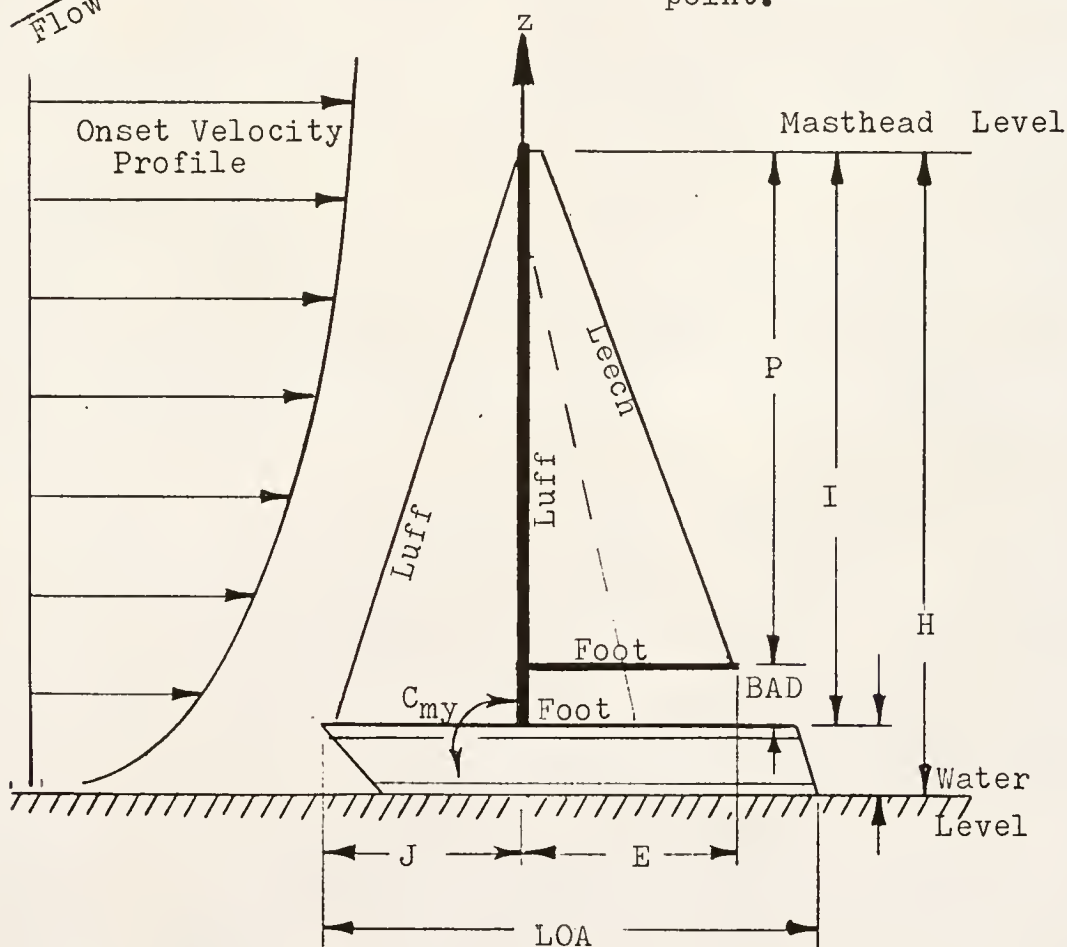
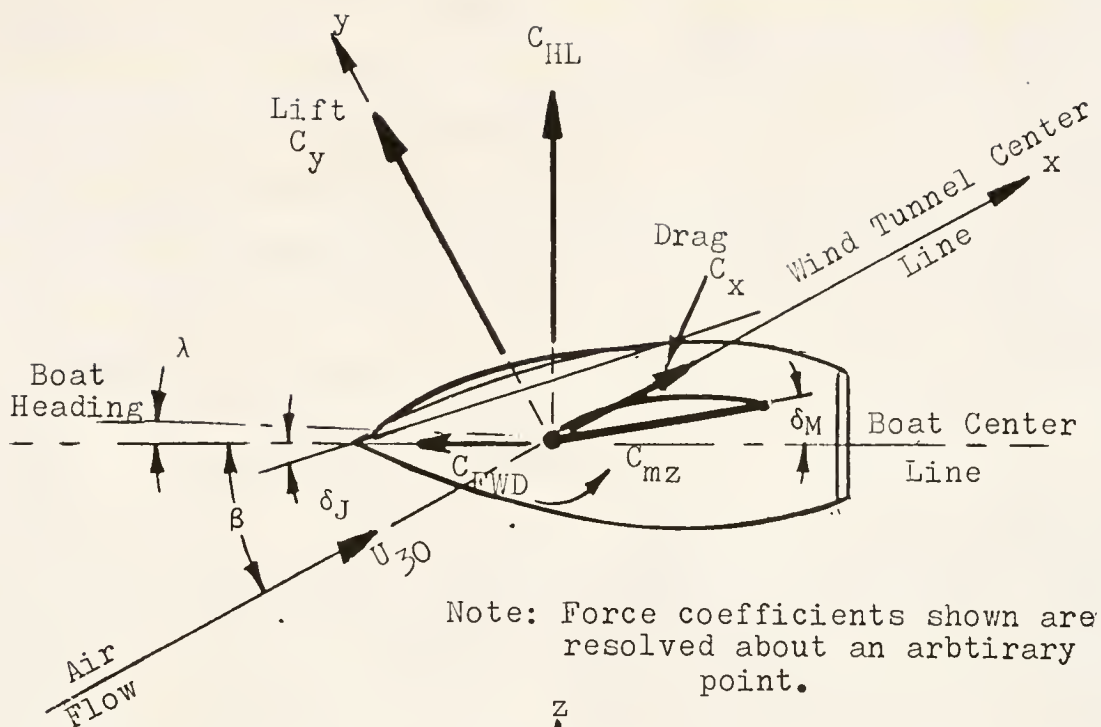


FIGURE 2-1 Coordinate System And Basic Dimensions

Where the non-dimensional arguments of this function are defined as

β = heading angle from the apparent wind to the boat center line

θ = heel angle

$U_T(z)/U_T(z_{MAX})$ = onset velocity profile normalized for z_{MAX} at the masthead

U_B/U_A = boat to apparent wind speed ratio

U_T/U_A = true to apparent wind speed ratio

$U_A c/\nu$ = inertial to viscous force ratio (Reynolds number, Re)

X' = rig parameters such as (cat, sloop, cutter, or ketch), vertical or raked masts), (standing rigging area to sail area), (free board area to sail area), (rig aspect ratio - $H^2/2SA$), (etc.)

$\overline{x}_S'$ = sail shape and trim parameters such as draft, draft position, foot curve, roach curve, sail dimension ratios (I/P, J/E ...), trim angles (δ_M, δ_J), twist parameters (ϕ_J, ϕ_M), etc.

L_t = dimensionless scale of turbulent

l_H = the geometric scale factor.

It is evident from Figure 2-1 that a smaller yacht "sees" a lower portion of the profile shown. Thus, scale similarity is dependent on the mast height (H) relative to the onset velocity function $U(z)$. Writing the relation between homologous model (MOD) and prototype (PROT) dimensions defines the geometric scale factor or

$$l_H = \frac{H_{PROT}}{H_{MOD}}$$

Application of the conditions of negligible leeway, zero heel, and zero boat speed yields

$$\theta=0, \frac{U_B}{U_A} = 0 \text{ and } \frac{U_T}{U_A} = 1$$

For the test results reported, the same sloop hull model with standing rigging is used except for those in which the sail is tested without a hull model. Therefore, in comparing one set of test results with another among the rig parameter variable group (X') the most significant variables are the rig aspect ratio ($H^2/2SA$) and freeboard to sail area ratio (FBA/SA). Only these will be reported.

Of all the sail shape and trim parameters ($\bar{x}_s'$) only those that are related to the overall planform shape and the sail trim adjustments furnished on the yacht model are reported. Sail parameters such as the foot and roach curves are proportional for each model and therefore are not listed. Similarly, the draft positions for cylindrical sails are constant and are located at the 50% chord point. As such, they are not parametrically varied and, therefore, are not reported. The sail shape and trim parameter group can then be written as

$$\bar{x}_s' = \bar{x}_s'(I/P, P/E, J/E, \delta_J, \delta_M, \phi_J, \phi_M, d_J, d_M)$$

and equation 2-1a becomes

$$C_F = C_F(\beta, R_e, H^2/2SA, \frac{FBA}{SA}, \bar{x}_s', L_t, l_H) \quad (2-1b)$$

Free stream turbulence is known to effect both laminar to turbulent transition and separation of the boundary layer attached to a lifting surface. Increased free stream turbulence normally results in increased drag and decreased lift. This phenomenon is an interactive one, it is thought to be a function of the scale of the free stream turbulence relative to the scale of the lifting surface turbulent shear layer (16, 17). Specifically, evidence suggests that if the free stream scale is large relative to that of the shear layer, the interaction is negligible. Referring to yacht sails, the free turbulence scale of the majority of the atmosphere is large; however, the scale of the lower regions of the air-water interface boundary layer is small. The actual and tunnel simulated velocity profiles are of classical logarithmic form. The portion of that boundary layer normally incident on the sails is generally within the "overlap layer" (18). In this region the scale of turbulence is proportional to elevation, z .

Investigation within the atmospheric boundary layer over open water by Ruggles (19) and later Groscup (20) included measurements of the Reynolds stress. Groscup (20) gives a value for the planetary boundary layer Reynolds stress per unit density of $0.328 \text{ ft}^2/\text{s}^2$ at a height near that of the prototype yacht midchord. This Reynolds stress was measured in a true wind velocity of 4 to 8 kt at a 30 ft elevation. This is compatible with the conditions of interest here. To compare this to the sail boundary layer, assume it to be that

of an equivalent flat plate so that a convenient length scale for comparison is the Prandtl mixing length (ℓ). Then using the one seventh power velocity distribution for the sail boundary layer, one can estimate that the maximum mixing length for the sails is about 0.2 in. at the trailing edge. A similar mixing length calculation, made for the planetary boundary layer, incorporating the measured Reynolds stress provided by Groscup gives a mixing length of about 30 in. Defining the prototype dimensionless parameter (L_t) for turbulence scale as

$$L_t = \frac{\ell_{PBL}}{\ell_{SAIL}} \quad (2-2)$$

gives for the velocity range of interest here $L_t = 150$. This suggests two orders of magnitude difference between the two scales. The preceding estimates refer to actual sailing conditions of a prototype yacht. If this were also the case for the model sail in the simulated wind tunnel boundary layer, then L_t could be eliminated from the pertinent governing parameters.

Turbulent intensity rather than Reynolds stress was experimentally measured in the wind tunnel. Thus to evaluate the same ratio, the Reynolds stress must be inferred from the turbulence intensity measurement. The correlation coefficient is defined by

$$\phi = \frac{\overline{u'v'}}{\sqrt{u'^2} \sqrt{v'^2}} \quad (2-3)$$

As suggested by Schlichting (16), it is taken as 0.45 in the overlap layer, and the lateral perturbation velocity $\sqrt{v'^2}$ is assumed directly proportional to $\sqrt{u'^2}$, consistent with the experiments performed by Reichardt (16). Given a measurement of ϕ for the mean velocity profile, one can evaluate the Reynolds stress using equation 2-3 with the above assumption and approximate the mixing length as

$$(\ell_{PBL})_m = \frac{\overline{u'v'}}{\partial u / \partial z} \quad (2-4)$$

where $\partial u / \partial z$ can be found directly from the measured mean velocity profile at an appropriate height (z), taken here as the height of the planform geometric chord. Equation 2-4 gives a model mixing length $(\ell_{PBL})_m$ of about 2.0 in. at $U_{30} = 80$ ft/s. The sail model shear layer is considered in the same manner as the prototype; this yields a sail model mixing length $(\ell_{SAIL})_m$ of 0.02 in. The dimensionless turbulence scale for the model $(L_t)_m$ is thus estimated to be about 100. This is sufficient to allow turbulent interactions to be ignored. As a result, equation 2-1b simplifies to

$$C_F = C_F(\beta, R_e, \frac{H^2}{2SA}, \frac{FBA}{SA}, \bar{x}_s', l_H) \quad (2-5)$$

The three-dimensional moments may also be written in coefficient form similar to equation 2-1a or

$$\frac{M}{\frac{1}{2}\rho U_A^2 SA C_{PF}} = C_m(\beta, Re, \frac{H^2}{2SA}, \frac{FBA}{SA}, \bar{x}_s', l_H) \quad (2-6)$$

where C_{PF} is the planform geometric chord. The moment coefficients represented by equation 2-6 are subject to the same dimensionless groups as those of equation 2-5.

For the experiments reported here, U_A is taken as the wind speed measured at the masthead of the model and the three principal force coefficients (reference Figure 2-1) are

- C_x -- drag, parallel to and positive in the direction of the apparent wind
- C_y -- lift, perpendicular to the apparent wind and to the mast
- C_z -- heave, parallel to the mast and mutually perpendicular to C_x and C_y .

The corresponding moments C_{mx} , C_{my} and C_{mz} represent moments about the same xyz coordinate system. Referring once more to Figure 2-1, the forward and side (heel) force coefficients relative to the boat center line are then defined respectively (for $\lambda=0$) as

$$C_{FWD} = C_y \sin(\beta) - C_x \cos(\beta) \quad (2-7a)$$

$$C_{HL} = C_y \cos(\beta) = C_x \sin(\beta) \quad (2-7b)$$

Equations 2-7 will be used in later discussions regarding potential yacht performance.

Finally, only one size yacht model is correct relative to the fixed onset velocity distribution $[U_{MOD}(z)]$ used here. However, slight extensions of the scale factor (l_H) may be assumed without appreciable error. For example, to represent a 30 ft prototype mast height while maintaining the same velocity distribution, the scale factor would increase to approximately 13. Conversely, for a 26 ft prototype mast height, l_H becomes approximately 11. Corresponding to these stretched scale factors, the estimated error in the net forces is $\pm 2.0\%$ of the nominal 28 ft prediction. The uncertainty in predicted force center is $\pm 0.7\%$, while the moment uncertainty is $\pm 5.3\%$. The difference in average velocity that the sails would see is $\pm 1.0\%$. All of these percentages are within the estimated experimental uncertainty of the tests conducted. Accordingly, the test results presented here may be considered applicable within reported uncertainty to sailboats from 18 to approximately 26 ft LOA as a function of their mast height off the water.

CHAPTER THREE YACHT AERODYNAMIC FORCES AND THEIR MEASUREMENT

Resolution of Aerodynamic Forces and Moments

In steady flow, the net aerodynamic forces and moments experienced by the sails, rigging and above water hull of the sailing yacht are reacted by hydrodynamic forces on the keel, rudder and hull underbody surfaces. These aerodynamic forces and moments are transmitted to the sails, rigging and above water hull via nearfield airflow pressure distributions and viscous boundary layer shear stresses on every free surface exposed to the flow. From the equations for static equilibrium three components of force and three components of moment are needed to define the net reaction of all the applied aerodynamic loads. Therefore, the direct measurement of orthogonal force and moment triples at any preselected location, on or off the yacht, would yield the net aerodynamic reactions.

In order to assess the contribution that the aerodynamic forces make towards the balance and stability of a yacht it is necessary to know both the magnitude and the location of these forces. For this purpose, the center of effort is commonly defined as a point in space where the applied aerodynamic forces may be resolved so that the net aerodynamic

moment about this point is zero. Appendix B discusses, in detail, resolution of the sail aerodynamic loads and the experimental determination of the yacht's center of effort. In general, it is demonstrated in Appendix B that direct measurement of forces and moments relative to an arbitrary global origin will not uniquely yield the center of effort location. As such, the center of effort must be known a priori or must be estimated from some physical constraints of the experiment.

Common Measurement Methods

The measurement of forces and moments resulting from flow about the sailing yacht model may be accomplished by any of three different methods:

- 1) Measure the pressure distribution over the free surfaces (Equation B-2) and sum the measurements as a function of area (Equation B-3);
- 2) Survey the upstream and downstream flow for momentum difference and evaluate the stream-wise static pressure distribution upstream and downstream of the model (valid for steady flow only);
- 3) Measure the three orthogonal components of force and moment via a force transducer or balance directly.

Pope and Harper (21) discuss each of these methods in detail relative to wind tunnel tests of aircraft. They indicate that three forces (lift, drag and yaw) and their companion moments completely describe the local spectrum of interest for aircraft. Symmetry and aircraft design experience indicate

that the point of resolution for these forces and moments is normally considered to be known a priori. These restrictions admit a unique solution for the aircraft. However, as discussed in Appendix B, this is not the case for the sailing yacht.

Most sails, including those tested, are thin. As a result surface orifices for pressure measurement are impractical. Wake survey methods are of use but require the measurement of both static and dynamic pressure at each survey point. Hence, the accuracy of any calculated force from such a survey is a function of the number of survey points selected. In order to reduce the number of measurements, it is common practice to measure only stagnation pressure at each of the survey points and to measure the static pressure at the wind tunnel wall. It is then assumed that this static pressure is uniform across the section of the tunnel where the measurements are made. This assumption is valid providing the distance downstream from the model is sufficiently far to allow static pressure recovery. Unfortunately, this is not often possible due to changes in wind tunnel geometry downstream of the test section. These can cause appreciable wall effect losses that must be accounted for with the consequence of an increase in the level of experimental uncertainty. Direct measurement with a force balance is the simplest and most straightforward method of attaining net force and moment data. However, as discussed above, specific details as to local force and moment distribution are not possible.

In the present work, a six-component force balance has been used to measure the aerodynamic response of various model sailing yacht rig configurations. A general discussion on six-component force balances is given in Appendix C with details of the six-component floating beam balance used here. The net maximum uncertainty for each force and moment coefficient range discussed in Appendix C is calculated employing the methods of Holman (22) and is given in Table 3-1. In addition to these direct measurements, the static pressure through the wind tunnel test section was surveyed both upstream and downstream of the model to establish correction parameters for solid blocking and horizontal bouyancy.

TABLE 3-1
Experimental Force and
Moment Coefficient
Maximum Uncertainties

Coefficient	C _x		
Range	0.0 to 0.2	0.2 to 0.4	0.4 to 1.0
Uncertainty	±4.3%	±3.9%	±3.1%
Coefficient	C _y		
Range	0.0 to 0.5	0.5 to 1.0	1.0 to 1.5
Uncertainty	±6.0%	±4.9%	±3.8%
Coefficient	C _z		
Range	-0.4 to -0.2	-0.2 to -0.1	-0.1 to 0.0
Uncertainty	±2.0%	±2.1%	±2.2%
Coefficient	C _{mx}		
Range	-3.0 to -2.0	-2.0 to -1.0	-1.0 to 0.0
Uncertainty	±4.8%	±5.8%	±5.9%
Coefficient	C _{my}		
Range	0.0 to 0.2	0.2 to 0.5	0.5 to 1.5
Uncertainty	±6.1%	±5.8%	±4.6%
Coefficient	C _{mz}		
Range	-0.2 to 0.2	0.2 to 0.4	0.4 to 0.6
Uncertainty	±3.1%	±2.6%	±2.1%

CHAPTER FOUR EQUIPMENT AND APPARATUS

The Wind Tunnel

The most essential piece of equipment used for the tests reported herein is the subsonic wind tunnel at Embry-Riddle Aeronautical University, Daytona Beach, Florida. This wind tunnel is a closed circuit, vertical, single return design which has an enclosed, low-speed and high-speed test section. Flow is produced by a 6-blade, fixed pitch, laminated wood propeller, 56 in. in diameter, that is driven by a 385 horsepower, 8-cylinder internal combustion engine. Speed control is provided by throttle and a 3-speed fluid drive transmission. Figure 4-1 illustrates the overall dimensions, general arrangement and location of the main features of interest.

The operational range of the tunnel is from 0 to 190 ft/s, 0 to 96 ft/s in the low-speed and 0 to 190 ft/s in the high-speed test sections. The low-speed test section is octagonal, 36 in. high by 52 in. wide, with a cross-sectional area of 11.5 ft^2 . To permit easy access to the model area and force balance, the entire low-speed section is mounted on a wheeled frame and can be removed from the tunnel proper. The six-component floating beam balance was installed on the under side of this test section, external to the tunnel

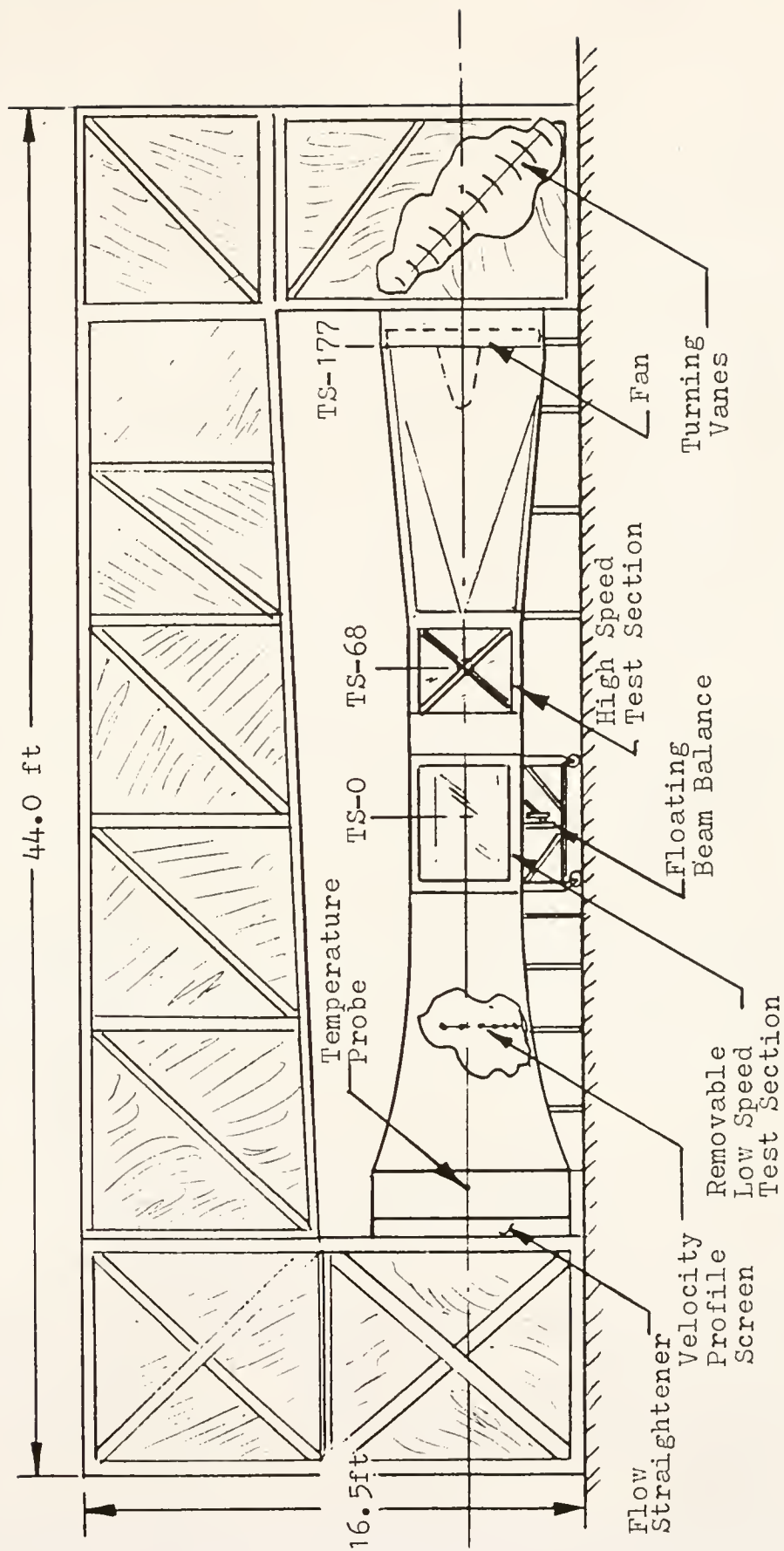


FIGURE 4-1 ERAU Subsonic Wind Tunnel General Arrangement.

interior. The rectangular high-speed test section is 24 in. wide by 36 in. high with an area of 5.96 ft^2 . Considering the effects of solid blocking together with the size of model needed for reasonable scale similitude, the low-speed test section allows the most flexibility and was used for all the results presented.

The low-speed test section was designed with $1/2$ degree diverging walls to maintain a constant streamwise static pressure through the section as the wall boundary layer thickens in the streamwise direction. For the range of velocities of concern here, the turbulence intensity of the mean flow is less than 0.004 and is considered well within acceptable limits for low-speed testing. The velocity distribution through the test section is uniform to within $\pm 1\%$. This, of course, was intentionally modified along with the turbulence intensity to produce the model velocity profile discussed in the next chapter.

Data Collection and Reduction

The experimental data presented were collected with the six-component floating beam force balance discussed in Chapter Three via a ten-channel Baldwin-Lima-Hamilton (BLH) Model 225 Switching and Balancing Unit and interpreted with a BLH Model 120C Strain Indicator. Each of the flexural elements of the force balance was fitted with a matched pair of Micro-Measurement EA-06-250BG-120 precision 120Ω gauges which were wired as two arms of a four-arm Wheatstone bridge circuit on

the Model 225 Switching and Balancing Unit for each of the six channels monitored. The remaining bridge circuit was completed using two precision 120Ω "dummy" resistors. Strains were read directly from the Model 120C Strain Indicator in microinches per inch, $\mu\text{in./in.}$ The backup and calibration check unit used for the BLH system was a Vishay Instruments BAM-1 Bridge Amplifier and Meter. System calibration checks were made monthly to ensure that experimental accuracy was maintained.

Flow air temperature was measured upstream of the low-speed test section at the inlet contraction cone by a mercury bulb thermometer probe connected to an external dial-type indicator which reads to $\pm 1^\circ\text{F}$ precision. Flow velocities were inferred from direct measurement of the local static and dynamic pressure with a common Pitot-static tube and a 50 in. water manometer. A Thermonetic Corporation HWA-101 hot wire anemometer was used for backup and to augment velocity survey measurements.

The force balance calibration equations were incorporated into a Fortran computer program along with all the pertinent flow relations for Reynolds number, yacht rig configuration sail area calculation, wind tunnel solid blocking and horizontal buoyancy corrections. The strain gauge resistance data for all six strain channels at each heading angle tested were loaded to a computer disc file. In addition, flow air temperature, the static and dynamic pressure at the masthead and the

rig configuration geometry were stored in this file. All of this information was subsequently processed through the Fortran program BOAT (a listing BOAT is given in Appendix D) on a Hewlett Packard HP-1000 mini-computer. A sample of the typical output information is shown in Table 4-1. The xyz-coordinate system is global; x being in the flow direction and z vertical while the boat coordinates of "Drive", "Heel" and "Vertical" are those as illustrated in Figure 2-1.

TABLE 4-1
Typical Output Format
From "BOAT"

RU,BOAT
INPUT NAME OF FILE RUN DATA IS STORED IN
(UP TO SIX CHARACTERS, FIRST CHARACTER START IN COLUMN 1,
JUSTIFIED LEFT, INCLUDE TRAILING BLANKS, IF ANY)
T15MH1

WIND TUNNEL DATA REDUCTION PROGRAM

SAIL BOAT DATA INPUT

INPUT SAIL DIMENSIONS (for model) I J P E (inches)
25 8 22.5 9

INPUT PERCENT OVERLAP, FRACTIONAL RIG FACTOR
1.5 1

WIND TUNNEL EXPERIMENT, DATA REDUCTION OUTPUT

TEST MODEL : SAIL BOAT

7/12/82 STD 150 SLP, W/PBL, FLOOR SEALED, STD ASPECT RATIO.

SAIL DIMENSIONS :

I = 25.00 J = 8.00 P = 22.50
E = 9.00

SAIL AREA (ACTUAL) = 1.82ft**2
SAIL AREA (100 % F.T.) = 1.40
PERCENT OVERLAP = 1.50
FRACTIONAL RIG FACTOR = 1.00

TABLE 4-1 -continued

SAIL ANGLES(DEG), VELOCITY(FPS), REYNOLDS NUMBER, DYNAMIC FORCE

COURSE ANGLE	Delta jib	Delta main	VELOCITY	REYNOLDS NUMBER	DYNAMIC FORCE
8.0	12.0	0.0	78.41	402566.	12.50
15.0	12.0	0.0	79.36	407417.	12.78
20.0	12.0	0.0	80.06	411020.	13.01
25.0	12.0	0.0	81.35	417675.	13.44
30.0	12.0	0.0	81.50	418426.	13.46
35.0	12.0	0.0	82.01	421015.	13.58
40.0	12.0	0.0	82.34	422739.	13.69
46.0	12.0	0.0	82.71	424646.	13.81

FORCE AND MOMENT COEFFICIENTS RELATIVE TO THE WIND

COURSE ANGLE	Cx	Cy	Cz	Cmx	Cmy	Cmz
8.0	.2607	.3778	-.0427	-.0800	.0569	.0769
15.0	.2727	.6533	-.0773	-.3655	.0698	.0538
20.0	.3207	.8607	-.1086	-.6114	.0727	.0206
25.0	.3542	1.1499	-.1402	-.7936	.1124	-.0032
30.0	.4592	1.3206	-.1672	-.9597	.1241	-.0430
35.0	.5641	1.4165	-.1878	-1.1360	.1755	-.0571
40.0	.7103	1.4891	-.2095	-1.0961	.2723	-.0408
46.0	.9575	1.4652	-.2202	-1.1495	.4645	-.0164

FORCE AND MOMENT COEFFICIENTS RELATIVE TO THE BOAT

COURSE ANGLE	DRIVE COEF.	HEEL COEF.	VERT. COEF.	HEEL MOMENT COEF.	PITCH MOMENT COEF.	YAW MOMENT COEF.
8.0	-.2056	.4104	-.0427	.0871	.0675	.0769
15.0	-.0943	.7016	-.0773	.3711	.1620	.0538
20.0	-.0070	.9185	-.1086	.5994	.2774	.0206
25.0	.1649	1.1918	-.1402	.7668	.4373	-.0032
30.0	.2626	1.3733	-.1672	.8932	.5873	-.0430
35.0	.3503	1.4840	-.1878	1.0313	.7953	-.0571
40.0	.4130	1.5973	-.2095	1.0148	.9132	-.0408
46.0	.3887	1.7066	-.2202	1.1327	1.1495	-.0164

CHAPTER FIVE SYNTHESIS OF THE ONSET VELOCITY PROFILE

Motivation and Basic Approach

The sailing yacht is subject to a non-uniform onset velocity resulting from the vector addition of the atmospheric planetary boundary layer (PBL) and the yacht's forward velocity. This summation produces what is termed the yacht's apparent wind. The magnitude of this apparent wind varies as a function of vertical height while its direction tends to rotate aft from the deck to the masthead of the yacht. Proper modeling of this boundary layer with respect to a sail rig configuration is essential for achieving scale similitude. Previous tests of sails and rig configurations (8, 9, 10, 11, 13) both two- and three-dimensional, have not used such an onset velocity. As such, conclusions regarding lift, drag and center of pressure could be significantly different than with non-uniform onset.

Modeling both the variation and direction in the wind tunnel is difficult. Modeling only magnitude variation with respect to one geometric axis is commonplace. Well developed methods for modifying a wind tunnel velocity profile exist (23, 24, 25). When the size of a particular wind tunnel is fixed in shape upstream of the test section, methods which develop a desired velocity distribution by artificial fetch

(roughness pegs, counter jets or, simply, the length of upstream convergent section) must be discarded. A more direct approach in such cases is to progressively impede the flow in the geometric direction of the desired velocity variation. That is if

$$U = U(z) \quad (5-1)$$

the direction of impedance (I) would also be

$$I = I(z) \quad (5-2)$$

This simple approach implies that a desired distribution could be synthesized by physically obstructing the flow upstream of the test section. In principle this is correct. However, the level of turbulence introduced into the flow due to the upstream obstruction(s) must be within some predetermined limits. Similarly, the pressure drop across the obstruction equates to an overall reduction in tunnel operating efficiency. With these considerations in mind, a direct impedance scheme can be planned to provide a desired velocity distribution and a reasonable turbulence intensity in the wind tunnel test section.

The Model Atmospheric PBL and Scale Effects

The measurement and formulation of the atmospheric boundary layer above the ocean has been the topic of numerous research efforts (19, 20, 26, 27). For modeling such a boundary layer in the wind tunnel with respect to the

sailing yacht, only the first 100 ft, measured from the water surface, is of interest. In this first 100 ft, often referred to as the surface layer, the characteristics of the actual PBL that are of significance towards such testing, for steady flow, are as follows:

- The wind direction is essentially constant and Coriolis effects may be ignored.
- The long duration vertical velocity distribution is logarithmic in form to within a 95% certainty.
- The scale of turbulence in the PBL is large relative to that of the sail boundary layer (this is verified in Chapter Two).
- Vertical variation of stresses and other fluxes can be neglected.
- The friction velocity (u^*) can be assumed a linear function of the wind speed rate of shear and is defined as

$$u^* = \sqrt{\frac{\tau_w}{\rho}}$$

where τ_w is the shear stress at the boundary (water plane) and ρ is the local flow density.

Neglecting convective terms, the generally agreed-upon form of the long duration, stable surface layer PBL is

$$U(z) = \frac{u^*}{K} \ln\left(\frac{z}{z_o}\right) \quad (5-3)$$

where z = vertical distance above the water plane

$U(z)$ = velocity at height z

K = von Karman Constant taken to equal 0.42

z_o = roughness length

From this form Kerwin (28) has developed a useful average wind gradient formula applicable to the sailing yacht in terms of the wind velocity at a height of 10(m) above the water surface [U_{10}] and the vertical position (z) for any profile velocity as

$$U(z) = 0.1086 U_{10} \ln[304.8z(m)] \quad (5-4)$$

Since equation 5-4 was that adopted by Register in his vortex lattice scheme, it will similarly be the profile shape synthesized in the wind tunnel.

As mentioned in Chapter Two, similarity requires that a specific size yacht be determined for test that simultaneously sizes an explicit portion of the PBL which must be synthesized. Converting equation 5-4 to scale ($l_H = 12$) yields

$$\frac{U(z)}{U_{30}} = 0.1086 \ln z(\text{in.}) + 0.4918 \quad (5-5)$$

where z is measured above the wind tunnel floor. Equation 5-5 is considered the desired shape of the scale velocity profile to be synthesized in the wind tunnel.

Profile Synthesis

The details of the direct impedance method used to synthesize the velocity profile described by equation 5-5 are given in Appendix E. The profile obtained by this method

fits the desired profile to a confidence of 96.5% using a least squares logarithmic curve fitting scheme on the measured velocity values. The turbulence intensity distribution through this synthesized profile has also been measured and it has been found to be in reasonable agreement with classical turbulent boundary layer measurements.

CHAPTER SIX YACHT MODEL DESIGN

Planform Geometry

As discussed previously, the overall model dimensions were selected as a function of the wind tunnel test section size relative to the type of yacht data desired. Consistent with popular sailcraft nomenclature, the primary geometric dimensions of the sail planform are I, J, P, and E as illustrated in Figure 2-1. The right triangle formed by the horizontal "J" and vertical "I" dimensions is termed the sail planform "fore-triangle". The vertices of any foresail and the edges between these vertices are shown in Figure 6-1. The luff perpendicular (LP) is drawn from the luff to the clew as shown. Foresails or headsails which exceed the fore-triangle area are named or "rated" as a percent of "J". Ergo the 150% genoa headsail is a sail with $LP=1.5J$. The region of the sail area which extends beyond the J dimension is termed the "overlap". For the research reported here, it is convenient to define the overlap ratio (OR) factor where

$$OR = \frac{LP}{J}$$

Similarly, sloop rigs which have fore-triangles that do not extend to the vessel masthead are termed "fractional

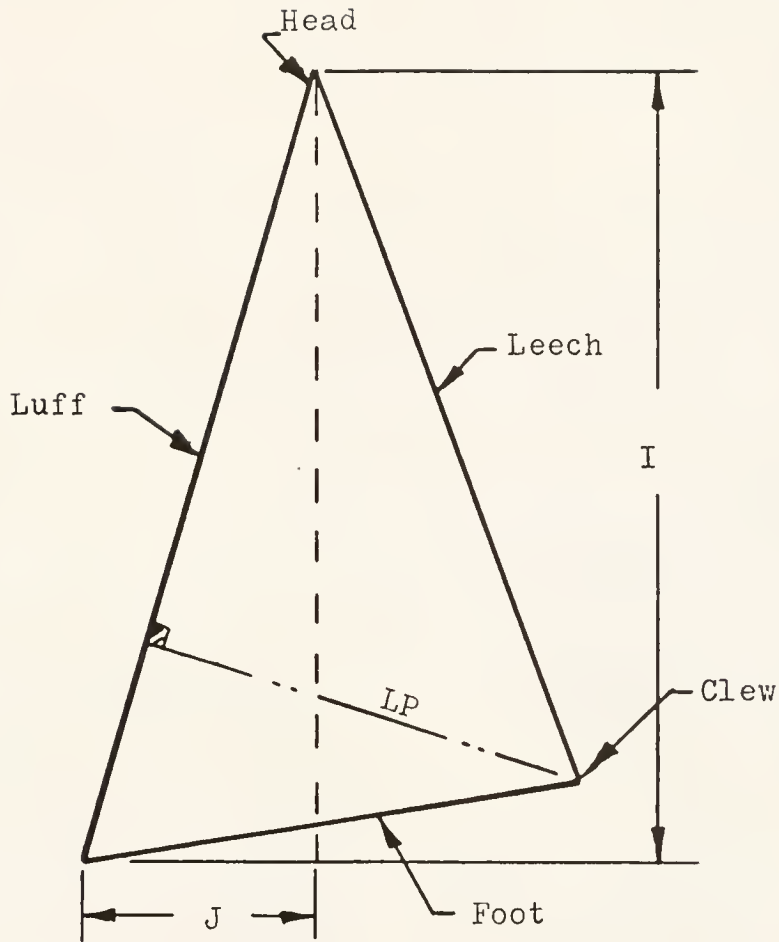


FIGURE 6-1 Sail Geometry Nomenclature.

rigs". To classify these geometries, one can define a fractional rig factor (FRF)

$$FRF = \frac{(I)}{(P+BAD)}$$

where (P+BAD) is the distance from the yacht deck to the masthead and for convenience is set equal to H.

The foresail triangular area (SA_F) can then be written as

$$SA_F = \frac{1}{2}(I^2 + J^2)^{\frac{1}{2}} \text{ (OR) } (J)$$

The mainsail dimensions are designated P and E. Its triangular area (SA_M) is

$$SA_M = \frac{1}{2}PE.$$

The rig planform area used for all of the force and moment coefficients is the sum of the foresail and mainsail area

$$SA = SA_F + SA_M$$

This area is slightly smaller than the actual sail area due to the curvature or "roach" of each sail's trailing edge. It is common practice to omit this area when defining sail area.

Rig aspect ratio is another quantity of interest and is somewhat more arbitrary in definition. The classical definition of aspect ratio used in the sailing community is

defined by the particular sail being referenced. The main-sail aspect ratio is taken as

$$AR_M = \frac{P}{E}$$

while the fore-triangle aspect ratio is

$$AR_J = \frac{I}{J}$$

For the present work, it is desirable to use an aspect ratio that adequately represents the entire rig configuration reflecting both mainsail and jibsail aspect ratios. For this purpose, a rig aspect ratio is defined here (for $H=P+BAD$) as

$$AR = \frac{(H)^2}{2SA}$$

It is believed that this definition is compatible with the classical individual sail aspect ratio definitions while satisfying the need for a single representative value for the rig.

The mean planform chord length is taken to be

$$C_{PF} = \frac{1}{2} \left[\frac{SA_F}{I} + \frac{SA_M}{P} \right]_{Jib} ; C_{PF} = \left[\frac{SA_M}{P} \right]_{Main}$$

Test Reynolds numbers for each wind tunnel test run are calculated using this chord length. It is reasoned that C_{PF} represents a realistic average length for the evaluation of the flow related viscous forces as opposed to other characteristic lengths which could be offered. This is thought

to be significant when comparing such test results to other wind tunnel data, either sail or airfoil, where the Reynolds number characteristic length can easily be ascertained.

Table 6-1 lists the planform geometry of the rigs investigated. Comparing the mean planform chords for the different configurations permits an interpretation of the similitude scaling problems discussed in Chapter Two. Indeed, if the mean planform chord provides a representative flow Reynolds number and if the effect of Reynolds number variation on the aerodynamic performance of a rig is strong, then results for the catboat ($C_{PF} = 4.6$ in.) compared with those of a sloop ($C_{PF} = 5.3$ in.) for the same wind velocity at the masthead, could be quite different than at the same Reynolds number. This observation would, in effect, make the Reynolds number a significant design parameter and somewhat removes the regime-like conclusions often made for wing and airfoil theory.

There is strong evidence that both of the premises offered above are valid. For Reynolds numbers less than 10^6 , the viscous flow effects are strong and the amount of surface area exposed to the flow is increasingly important; that is, the force coefficients produced by a thin single cambered surface in the flow are non-linear and vary with small changes in Reynolds number. Figure 6-2, reproduced here from NASA CR1767 (10), is an illustration of this

TABLE 6-1
Planform Geometries

Configuration Reference Code*	Rig Description	Planform Nomenclature					
		FRF	I	J	P	E	PO
T00CB1	Finn-Type Sail	0.0	0.0	0.0	24.80	15.30	0.0
T15MH1	Sloop - Masthead	1.00	25.00	8.00	22.50	9.00	1.52
T15781	Sloop - 7/8	0.875	21.88	7.00	22.50	9.00	1.52
T15341	Sloop - 3/4	0.750	18.75	6.00	22.50	9.00	1.52
T15121	Sloop - 1/2	0.500	12.50	4.00	22.50	9.00	1.52
T0SCB1	Catboat	0.0	0.0	0.0	22.50	9.00	0.0
T15MJ2	Sloop - High AR Jib and Main	1.00	25.00	6.00	22.50	6.43	1.52
T15MJ3	Sloop - High AR Jib Alone	1.00	25.00	6.00	22.50	9.00	1.52

NOTE: Dimensions shown are in inches $\pm$ 0.016 in.
*Configuration Reference Codes are used throughout this work to cross reference test information.

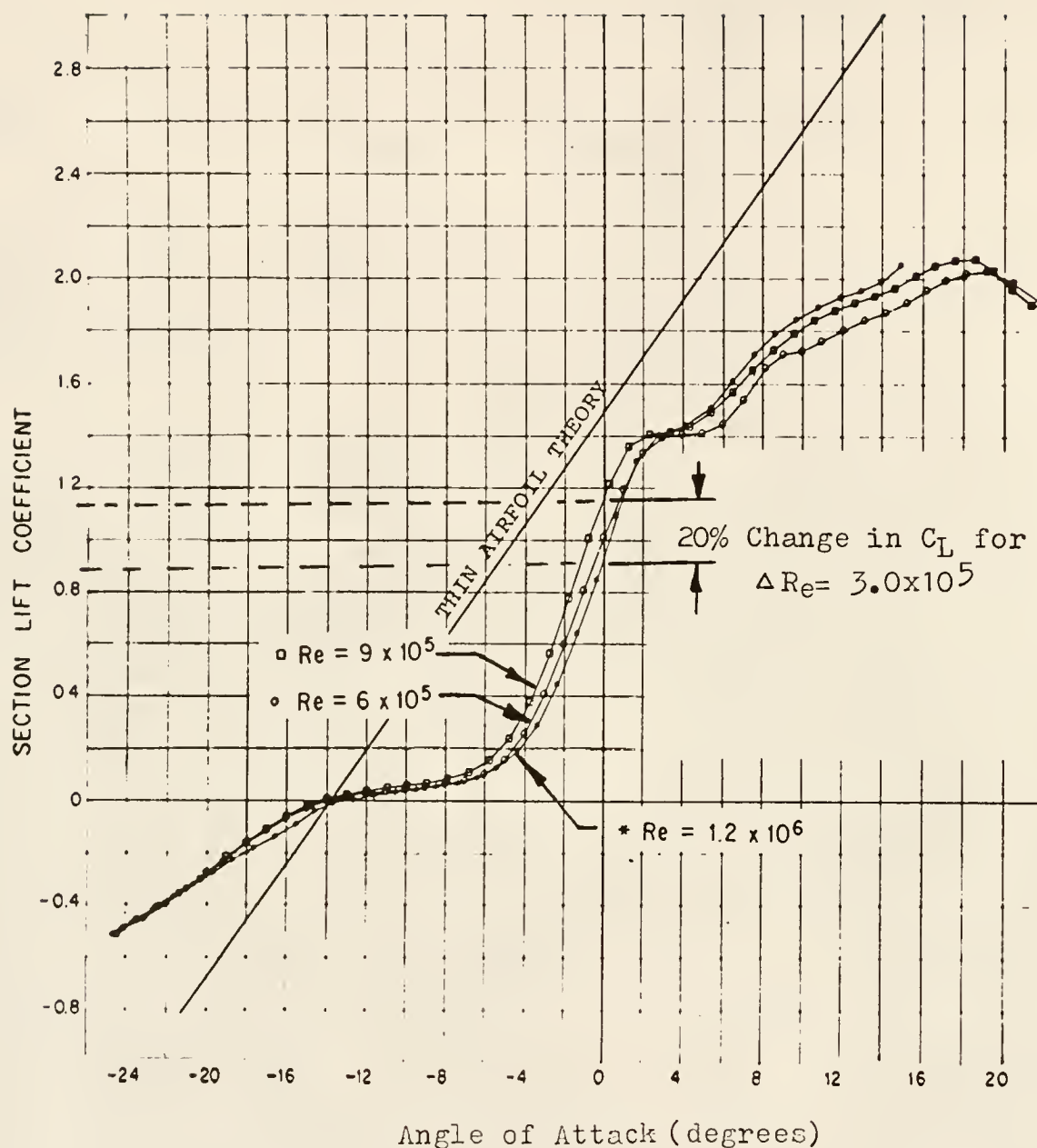


FIGURE 6-2 Lift Coefficient vs. Angle of Attack for a Thin, Single Cambered Section as Measured by Milgram (ref. CR1767) for Three Reynolds Numbers and as Predicted by Thin Airfoil Theory.

effect. For the sailor though, the point of interest is normally the rig performance at a given wind speed. Thus, increasing or decreasing the Reynolds number can play an important role as a practical performance parameter.

For the results presented here, comparisons are made for the same masthead velocity on each of the rigs tested. Each will, therefore, be at a different Reynolds number as a function of C_{PF} .

Model Geometry and Trim Adjustments

The geometric scale factor (l_H) is taken as 12 to permit a model size that is within acceptable limits relative to wind tunnel test section blocking requirements. This size also provides a fair representation of actual sailboats ranging from 18 to 25 ft LOA for the wind velocity profile being simulated. A scale mast height above the water surface of 28 ft is chosen to allow a reasonable fit with the planform geometries previously defined. Figure 6-3 gives the overall model and standing rigging dimensions. The different planform geometries are also shown for clarity.

Deck size is arbitrary and is selected to permit a reasonable arrangement of sail trim control and adjustment devices. The standing rigging acts as the structural support for the sail and consists of upper shrouds and a backstay. The upper shrouds are adjustable to maintain the mast perpendicular to the deck in the athwart-ship direction and to control the shroud tension. Backstay tension is also

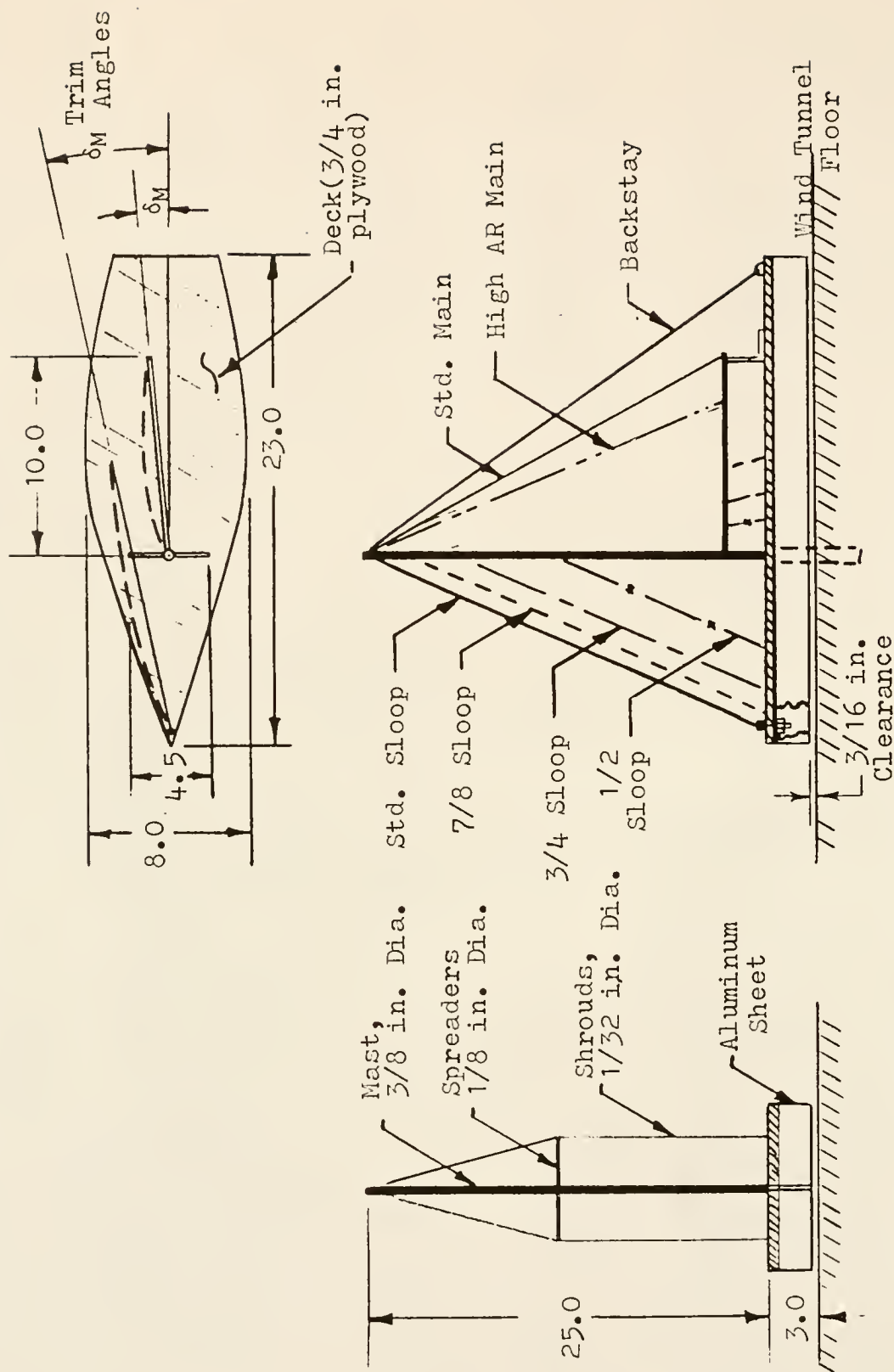


FIGURE 6-3 Wind Tunnel Yacht Model Configurations.
(All dimensions given are in inches)

adjustable and can be used to bend the mast in the fore and aft plane. The mast is rigidly fastened to the deck to prevent its base from rotating about the athwart-ship axis. No forestay is used. Instead, the metal fore-sail head is attached directly to the mast by a threaded fastener and the tack is connected to a through-deck adjustment screw. Tensioning the luff via this screw controls the fore and aft position of the masthead and opposes the backstay. In contrast to cloth sails, an increase in luff tensions on the metal sail does not move the sail draft forward and only the lower one-third to one-half of the sail draft is adjustable by means of sheet trim and clew downhaul. Upper section headsail draft is rolled into the sail prior to installation on the model and can only be altered by re-rolling. Headsail sheet trim is achieved by means of a threaded rod adjustment device that is fastened to the deck. When the length of the rod is reduced, it draws the sail clew aft. This decreases the trim angle and flattens the lower portion of the sail. Headsail leech downhaul is attained using a second device fastened directly to the sail clew and to the model deck. Therefore, the clew vertical position can be changed, secured and maintained throughout testing.

The mainsail boom is equipped with a traveler/downhaul that permits the trim angle and sail twist to be controlled adequately. A downhaul adjustment permits the leech tension

to be eased or tightened as needed. Finally, a mainsail clew may be trimmed independent of the trim angle or leech tension.

Sail Construction

All the sails are made of 2024-T3 aluminum sheet, 0.020 in. thick, cut to the planform described above. Each sail is then rolled parallel to its luff to produce a cylindrical surface such that any camber line drawn perpendicular to the sail luff is a circular arc or constant radius of curvature. The LP, the longest such camber line, and the sail draft depth (d) as a proportion of straight line chord length are used to define a characteristic radius of curvature or

$$r = \frac{LP}{2} \left[\text{ARCCOS} \left(\frac{1-4(d)^2}{1+4(d)^2} \right) \right]^{-1} \quad (6-1)$$

For the headsail, $(I^2+J^2)^{\frac{1}{2}}$ is taken as the cardinal surface ruling. Then the total surface is easily defined in cylindrical coordinates by equation 6-1 for the intervals

$$0 \leq \Lambda \leq 2 \text{ ARCCOS} \left[\frac{1-4(d)^2}{1+4(d)^2} \right]$$

and (6-2)

$$z'_{F_0} \left(1 - \frac{r\theta}{LP} \right) \leq z' \leq (I^2+J^2)^{\frac{1}{2}} - z'_{F_0} \left(1 - \frac{r\theta}{LP} \right)$$

where

$$z'_{F_0} = -J \left[\frac{1}{(I/J)^2 + 1} \right]^{\frac{1}{2}}$$

Figure 6-4 illustrates the coordinate system and the surface bounds. The z' coordinate system is rotated and translated relative to the previously described global coordinates. z'_F and z'_L are foot and leech bounds respectively. Equations 6-1 and 6-2 in the defined intervals of Λ and z' describe the cylindrical headsail in terms of its rated dimensions. Similar intervals can be written for the mainsail in terms of its P , E dimensions and the d proportion.

With the geometry of the sail defined as indicated, it is clear that the deepest draft point of each sail is located at 50% chord. While this is farther aft than suggested by current sailing wisdom for the headsail, it is approximately correct for the mainsail. This divergence from current sail set philosophy is considered acceptable for the comparisons made here, since it is consistently applied throughout all of the rig configurations tested and is similarly employed for the vortex lattice models discussed later. Certainly, such comparisons should establish performance trends and it seems probable that these trends should be preserved as the deepest sail draft point is moved moderately further forward.

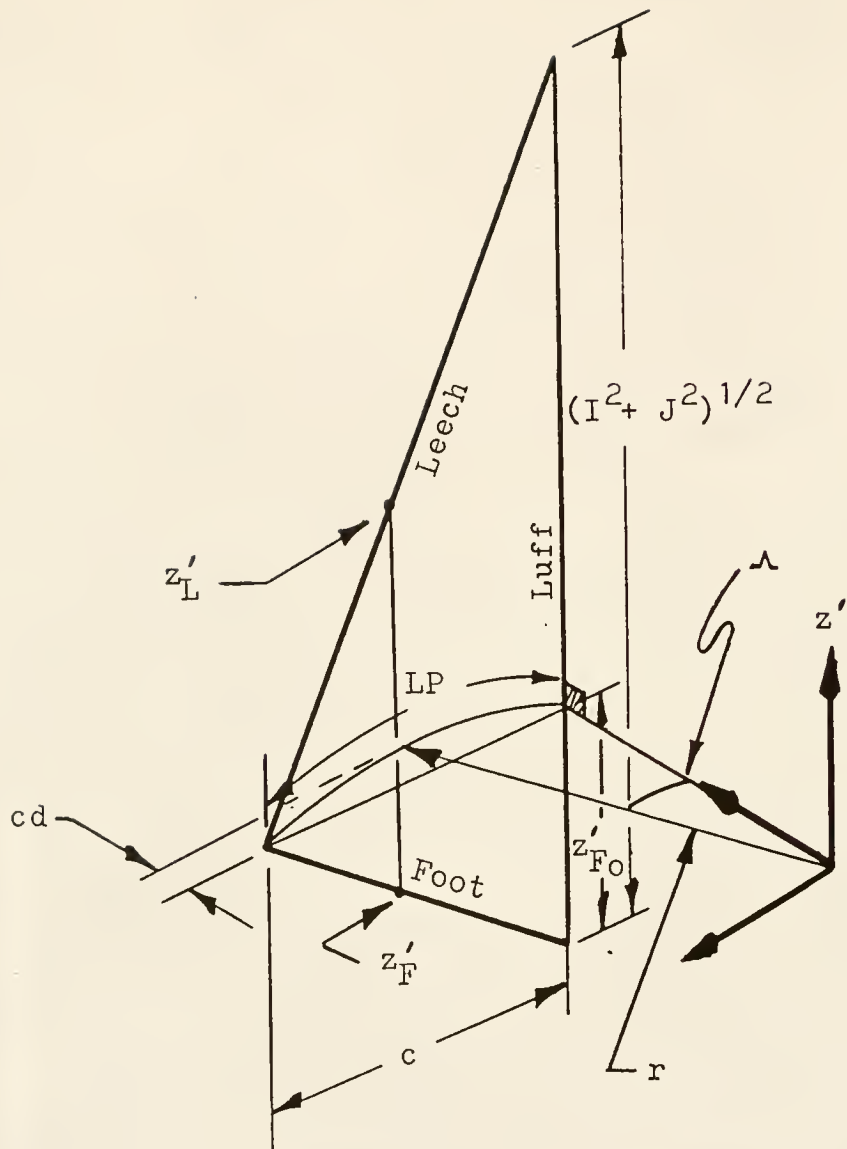


FIGURE 6-4 Sail Surface Description.

Sail Trim, Setting and Measurements

The model deck is equipped with two protractor scales; one to measure the headsail trim angle (δ_J) and one to measure the mainsail trim angle (δ_M) relative to the boat centerline. The trim angle is defined as the angle between the boat centerline and a line drawn from tack to clew along the foot of the sail in question. Each sail has camber lines drawn at selected z-locations as shown in Figure 6-5. Once the sail is fitted to the model, a sail "set" may be described by measuring the leech position of each camber line relative to the vertical plane that contains the foot trim angle line, thus yielding the sail twist. The draft position and depth of draft are measured directly from a line drawn from the camber line luff to the camber line leech. In total, these measurements provide a mapping of sail shape which characterizes a particular set.

Finally, each sail is equipped with tufts along both the luff and leech on both windward and leeward sides. By direct visual observation of tuft motion, any sail set may be assessed for attached or separated flow. This visual observation is useful prior to test data collection to ensure that reasonable trim has been attained and that no large scale flow separation exists over the planform.

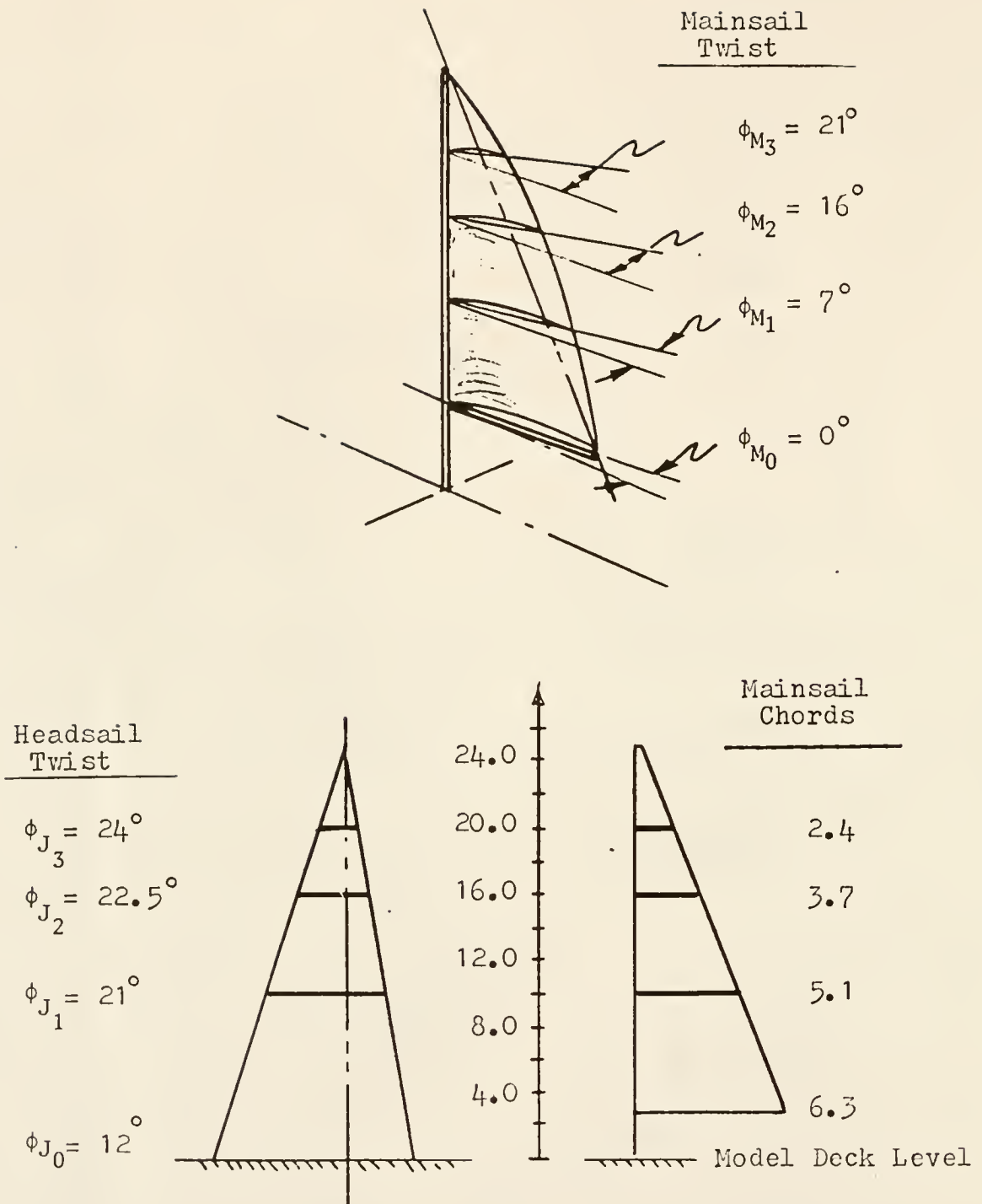


FIGURE 6-5 Typical Sail Camber Lines, Draft and Twist Measurement Locations.
 (Dimensions shown are in inches.)

CHAPTER SEVEN COMMON EXPERIMENTAL PROCEDURE

Sail Trim Pre-Test

Prior to each configuration test, the model was placed in the wind tunnel with an approximate sail set and trim. A first order approximation of trim was established at pre-selected trim angles for the jib and main with draft and twist selections estimated from sailing experience and judgement. An observation test run was then conducted to assess the twist and draft set. The sails were inspected for windward or leeward separation. For this purpose, a large mirror was installed on the far wall of the wind tunnel test section; this allowed simultaneous observation of both sides of the sails. If flow separation was noted from the motion of the sail tufts, appropriate trim adjustments were made. Such adjustments were analogous to their counterparts of the full scale yacht, each adjustment having the corresponding effect. For example, if large separation was noted aloft on the leeward side of the jib, a correction was to ease the leech downhaul which induced increased sail twist. This is analogous to moving the jib sheet fairlead aft on a full scale sailboat which relaxes leech tension and increases twist on the upper portions of the sail.

With the wind tunnel operating at approximately the speed of the intended test, the model was slowly rotated through a range of heading angles from approximately 10° to 45° . Leading edge and trailing edge separation points were noted using the tufts as indicators as a function of β and a few strain gauge resistances were recorded at the maximum y-force point. Small adjustments were then made to the trim to maximize the lift force as indicated by a maximum resistance reading. The model was rechecked for leading and trailing edge separation heading angles. Finally, the wind tunnel was shut down, the model removed and sail trim measurements were recorded (reference Table 7-1).

Wind Tunnel Test

The model was reinstalled in the wind tunnel and the force balance was nulled on each of the six channels with the model set at a heading angle of $\beta=10^{\circ}$. The model was rotated through 45° to indicate if any weight balancing was necessary to maintain each of the six channel null points. Variations in resistances that correspond to more than 5µin. were corrected by the addition or removal of weights from the model. The correct model center of gravity having been determined relative to the force balance, the wind tunnel was started and brought up to test speed. Raw test data were recorded manually for each channel at each heading angle and checked for repeatability in an up-down reading sequence. Heading angles were indexed in approximately 5° increments from 10° to 45° .

TABLE 7-1
Mean Sail Shape and Trim
Parameters

Configuration Reference Code	δJ	$\bar{\phi}_J$ (Per Span)	$\bar{d}_J$	δM	$\bar{\phi}_M$ (Per Span)	$\bar{d}_M$
T00CB1	N/A	N/A	N/A	0°	32°	11%
T15MH1	12°	16°	12%	0°	30°	12%
T15781	12°	18°	10%	0°	31°	12%
T15341	12°	20°	11%	0°	33°	11%
T15121	12°	18°	11%	0°	35°	12%
T0SCB1	N/A	N/A	N/A	0°	36°	13%
T15MJ2	12°	14°	10%	0°	22°	7%
T15MJ3	12°	14°	10%	0°	24°	11%

During each test, the static and dynamic pressure of the flow at the calibration point (3.00 in. above the model masthead) were noted along with the airflow temperature. At the end of each test, the force balance was checked for zero return. Any zero drift error found was investigated. If the error was in excess of 5µin., that set of test data was discarded. For such cases, the same configuration was rerun.

Each configuration test required approximately two hours of wind tunnel run time. After each run, a calibration check was made on the force balance and the calibration equations were adjusted accordingly.

CHAPTER EIGHT MODEL CONFIGURATION TEST RESULTS

The Finn-Type Sail Test

The Finn is a simple, one-design catboat rigged, sailing dinghy which offers an easily modeled sail planform (reference Table 6-1). Marchaj (8) has used a one-seventh scale ($l_H=7$) fabric scale model of the Finn sail to experimentally investigate the effects of boom vang tension and resulting sail shape on the total sail lift and drag coefficients. His test results were achieved under uniform onset flow conditions and they are reported at scale Reynolds numbers that are in the range reasonable for similitude.

In the present work, a geometric scale factor of $l_H=9$ yields a Finn model of appropriate size to fit the wind tunnel test section without severe blocking. This model was constructed absent of a hull and deck to permit an accurate determination of the sail force center both with and without a simulated non-uniform onset velocity. The non-uniform velocity profile that was used in these tests is described in Chapter Five and, thus, is slightly distorted relative to the portion of the true PBL that the full-size prototype Finn sail would actually "see". This distortion, however, is believed to be unimportant for the comparisons presented.

The sail planform is given in Table 6-1, Configuration Code T00CB1. The sail model was rolled to a cylindrical shape parallel to the luff and fastened to a cylindrical shape parallel to the luff and fastened to a mast/boom assembly. The mast/boom assembly was stiffened using a small spreader and shroud combination. This stiffening was necessary since the mast section modulus must be small in order to conform to the prototype mast. Its contribution to the resultant drag has been verified by measuring the drag of bare poles (mast and boom alone). This test indicated that the mast drag contribution was negligible. Camber lines at 4 in. intervals starting at the boom were used to define draft and twist. Since this model had no hull or deck, the adjustments of the sloop model tests were not available and the sail set capability was correspondingly limited. Angles of attack measured between the boom and the wind tunnel center line were used in place of heading angles. Partial sail twist control was realized by using a boom topping lift and vang but at a slight drag increase.

At large angles of attack, the stress on the sail was observed to be sufficient to slightly alter the initial sail twist. Naturally, the effect increased with increasing flow velocity. The same effects would occur on the prototype Finn but vang, sheet and mast bend have not been modeled proportionately for the wind tunnel test. Therefore, at large flow velocities (Reynolds numbers) there would be some departure from geometric similitude.

Direct flow observations on the windward side of the sail were of particular interest both with and without the PBL velocity profile. Figure 8-1 illustrates a side view of the model and the positions long yarn tufts assumed when placed in a 40 ft/s flow with the sail model at an angle of attack of 25° . Using the camber lines for approximate measurement, a yarn tuft was estimated to be near the windward side of the sail pressure center if it was not deflected up or down relative to the horizontal plane. This observations was made with the non-uniform logarithmic velocity profile in the tunnel and tuft #2, approximately $0.36 H$ above the floor, was estimated to be at the windward side center of pressure. Similar observations of the leeward side of the sail were not as informative because of large random yarn motion.

As expected, two strong vortices were seen; one near the masthead and one near the boom. A top view of the leeward side, Figure 8-2, reveals the strong upwash which was observed upstream of the leading edge and smooth flow to about the $3/4$ chord point where turbulence and the shed trailing edge vortex displaced the yarn. Near the $0.36 H$ height noted, an upwash angle of approximately 10° at the sail leading edge was measured relative to the wind tunnel centerline. This angle appeared to vary along the leading edge as a function of vertical position.

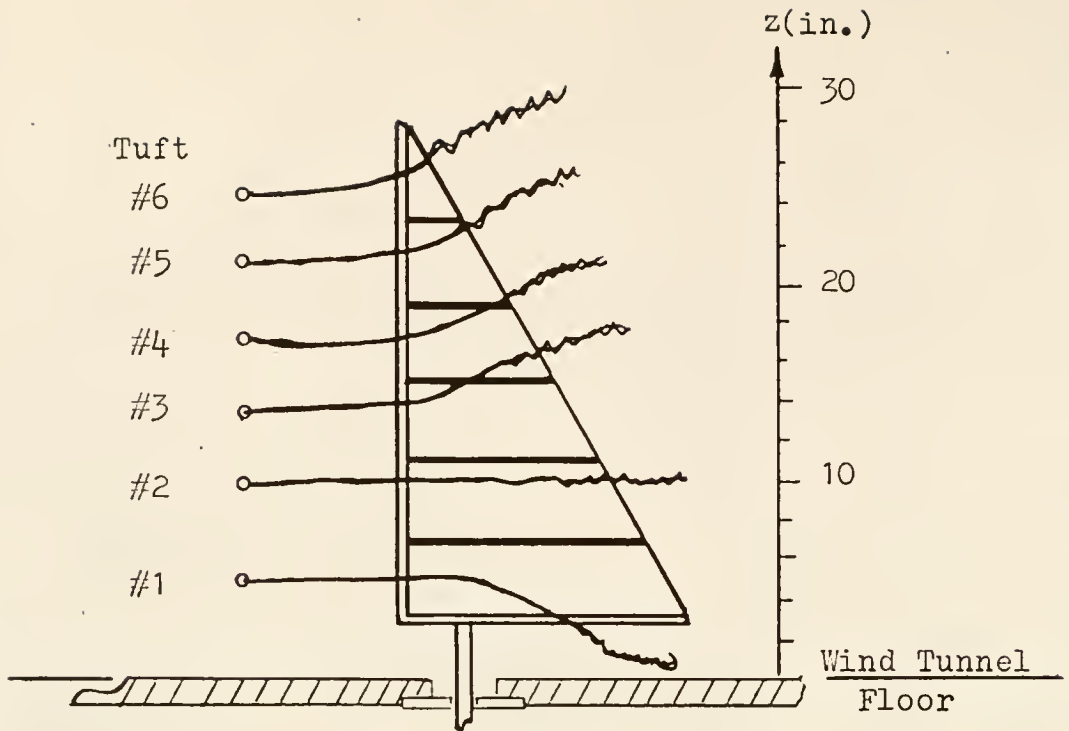


FIGURE 8-1 Finn Sail Yarn Observations.
Observed yarn motion for the Finn sail at an attack angle of 25° , $V=40$ ft/s, without the simulated PBL.

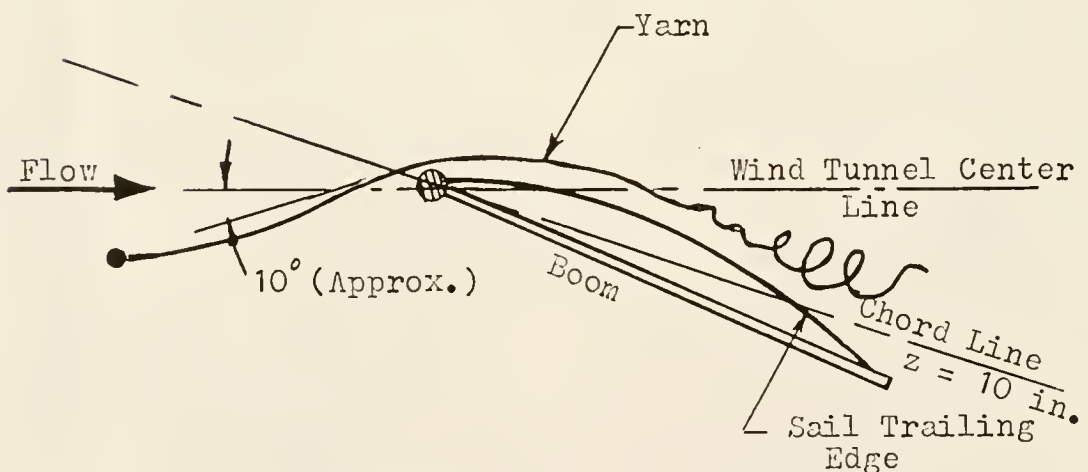


FIGURE 8-2 Finn Sail Upwash Observation.
Position assumed by a long yarn on the leeward side of the sail as viewed from above.

While the above observations come as no surprise, they do provide insight and evidence in support of concepts suggested later in this work. For instance, the strong upwash noted is in agreement with the arguments offered in Appendix A for quantifying the magnitude of relative wind twist that results solely from the sail circulation and is almost independent of boat speed.

The results of the force balance data collected for the Finn sail, both with and without the simulated PBL onset velocity profile, are given in Figure 8-3 as lift (C_x) and drag (C_y) coefficients along with the lift to drag ratio (C_y/C_x) plotted against the attack angle. Selected data points from Marchaj's work are superposed for comparison and are seen to be in fair agreement considering the large differences in models used (metal to fabric) and the unreported uncertainty of the earlier work. The second stall or "bump" seen in both C_y -plots at approximately 30° attack angle for uniform and logarithmic onset velocities is thought to be a result of sail trim changes caused by the pressure distributions in these high loading conditions. Both tests were conducted for a velocity of 80 ft/s at the model mast-head and a Reynolds number of 3×10^5 . The simulated PBL had the effect of decreasing the lift and drag values over those measured for uniform flow at the same attack angle.

As a point of interest, the negative vertical force coefficient is also plotted in Figure 8-4 for the case with

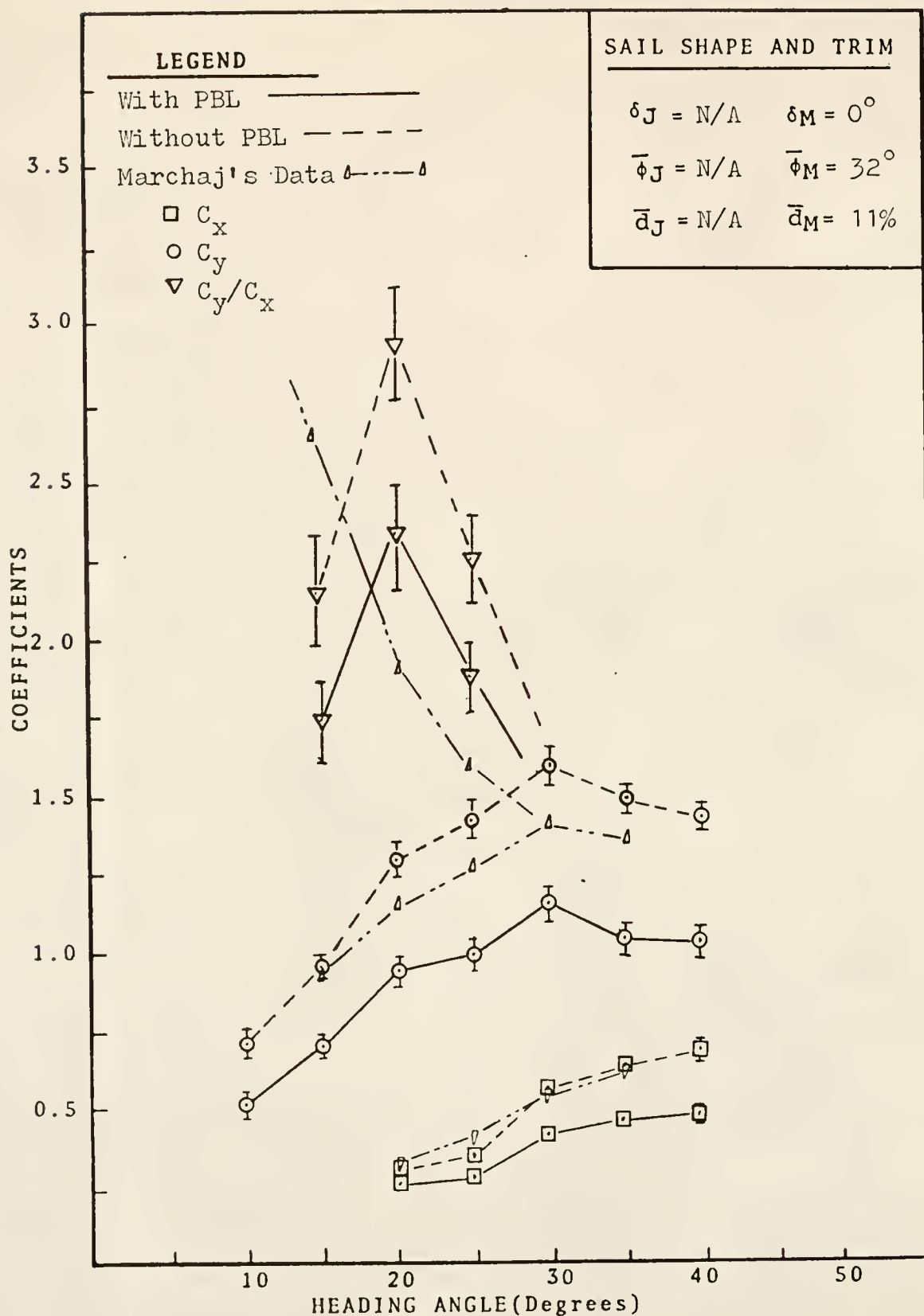


FIGURE 8-3 Effect of the Planetary Boundary Layer on Sail Test Data. Finn sail for $Re = 1.8 \times 10^5$ vs. Marchaj's data for $Re = 2.2 \times 10^5$.

the simulated PBL. Because of mast and sail deflection under load, a small projection of sail area can be obtained in the xy-plane. The centroid of this projected area is not necessarily the same as the planform centroid. The negative vertical force related to this coefficient (C_z) can be thought of as the net reaction of the spanwise flow momentum on this projected area. This interpretation is supported by the yarn observations made earlier and would also be true for the prototype Finn.

The x and y moment coefficients are shown in Figure 8-4. While the overall trends of the coefficients are preserved, the net effect of the non-uniform onset velocity is seen to reduce the values of C_{mx} and C_{my} .

In an attempt to resolve the sail force center, it is conservative to assume that each of the forces represented by the coefficients C_x , C_y , and C_z are concentrated at different x, y, z locations or

$$F_x = F_x(x_1, y_1, z_1)$$

$$F_y = F_y(x_2, y_2, z_2)$$

and

$$F_z = F_z(x_3, y_3, z_3)$$

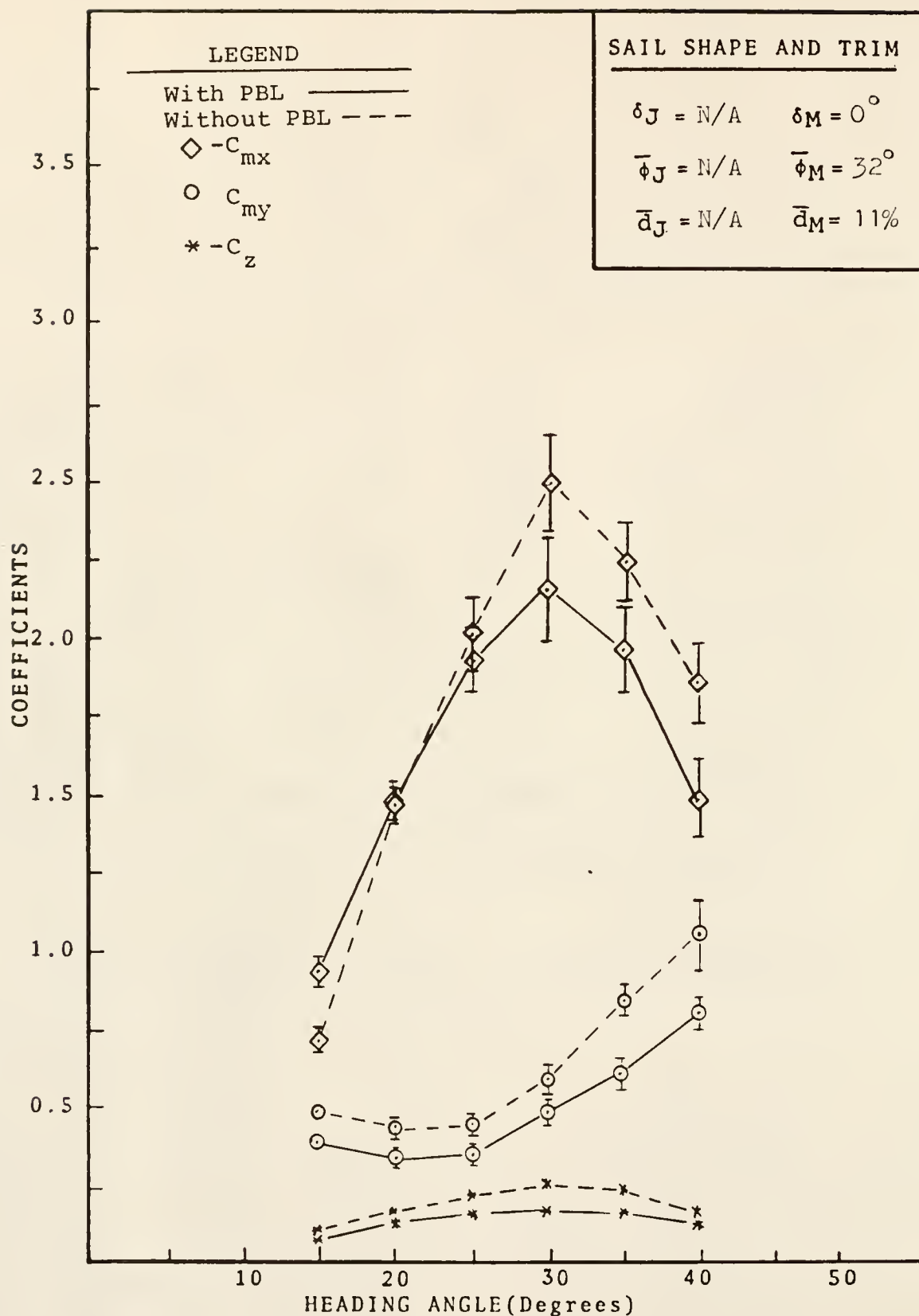


FIGURE 8-4 Finn Moment Coefficients With and Without the Planetary Boundary Layer.

If the vertical force is taken as the vertical component of the total lift vector, where the total lift vector is normal to the sail surface at its center of pressure, one obtains

$$x_2 = x_3 \quad \text{and} \quad y_2 = y_3 \approx 0 \quad (8-1)$$

The resultant moments of the three forces in coefficient form can be written as

$$\begin{aligned} C_{mz} &= (C_y x_2 + C_x y_1) / C_{PF} \\ C_{mx} &= (C_y z_2 + C_z y_3) / C_{PF} \\ C_{my} &= (C_x z_1 + C_z x_3) / C_{PF} \end{aligned} \quad (8-2)$$

Applying the conditions of equation 8-1 to equation 8-2 gives

$$z_2 = \frac{C_{mx} C_{PF}}{C_y} \quad (8-3)$$

From the plots in Figure 8-3 it is noted that

$$C_y > C_x$$

It is logical to assume $y_1 \leq x_2$, therefore, the z-moment coefficient can be approximated as

$$C_{mz} \approx C_y x_2 / C_{PF} .$$

Solving for x_2 yields

$$x_2 \cong \frac{C_{mz} C_{PF}}{C_y} . \quad (8-4)$$

Using equations 8-1, 8-2 and 8-4 the vertical height to the x-force location can be approximated as

$$z_1 = \left[\frac{C_{my} - C_z \left(\frac{C_{mz}}{C_y} \right)}{C_x} \right] \quad (8-5)$$

It is clear that the uncertainty of z_2 is less than that of z_1 which, in turn, is greater than the uncertainty of any one of the contributing coefficients. The uncertainty of z_2 is calculated as $\pm 7.87\%$ while z_1 is estimated at $\pm 11.09\%$. Using these uncertainties to generate weighting factors W_1 and W_2 , a sensible vertical distance to the force center is defined as

$$z(\text{nominal}) = \left[\frac{(W_1 z_1)^2 + (W_2 z_2)^2}{W_1^2 + W_2^2} \right]^{\frac{1}{2}} \quad (8-6)$$

Where $W_1 = 9.02$ and $W_2 = 12.71$ based on the above uncertainties. Since the plane of the projected sail planform rotates relative to the x,y,z-coordinated system, the horizontal distance from the mast to the force center is a function of x,y and the attack angle. Figure 8-5 shows the force center locations as calculated by equations 8-4 and corrected for the attack angle.

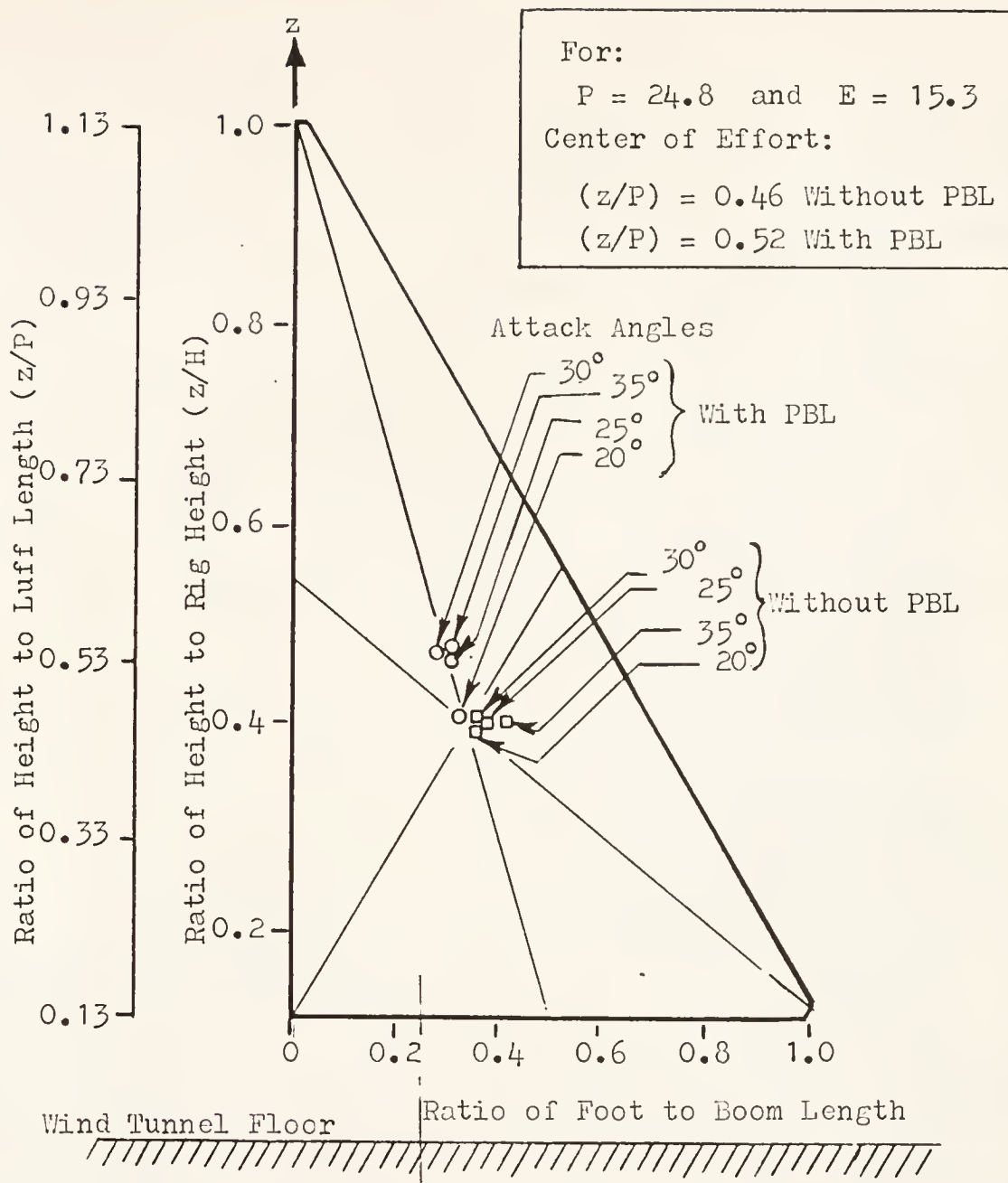


FIGURE 8-5 Finn Sail Center of Effort Location.

Center of effort locations at various attack angles with and without the simulated PBL.

Note: Because the PBL is distorted slightly for the model used the effect on the c.e. translation as shown is less than it would be for a correctly scaled PBL.

The concentration of points around the planform centroid for the uniform velocity is logical and suggests an analytical check using a direct integration scheme. Such a scheme is presented in Appendix D for both with and without the modeled PBL. Without the planetary boundary layer model the calculated center of effort is shown to be $0.39 H$, which is identical to the planform area centroid and agrees with the location indicated in Figure 8-5 for attack angles of 25° to 35° . With the non-uniform onset velocity the method requires an approximate model for the lift coefficient variation as a function of span position. Approximating polynomials are selected for this purpose to allow a convenient form for integration by parts. For the selected precision the results are nearly invariant with the assumed lift coefficient models and the net center of effort is shown to be $0.48 H$. This value is also in close agreement with the measured value as indicated by Figure 8-5.

Finally, the Finn model was used in an effort to assess the effect of Reynold's number on C_y values at or near stall. At an angle of attack of 30° , three tests were conducted corresponding to Reynolds numbers of 2.3×10^5 , 2.9×10^5 and 3.3×10^5 . The C_y values obtained show no variation beyond that of the expected uncertainty, see Table 3-1. The same results were found for C_x and C_{mx} values. Therefore within the range of Reynold's numbers investigated, this parameter's effect is negligible.

Variable Jibsail Hoist Series

Employing the full yacht model described in Chapter Six, a variable headsail hoist series of tests was conducted. The results of this series were intended to be compared with theoretically derived lift and drag coefficients. For this purpose, the yacht model was fitted with a mainsail ($P = 22.50$ in. and $E = 9.00$ in.), common to all tests. A series of jibsails, all of the same aspect ratio but with various hoists, was fitted to the model and tested as described in Chapter Seven. Tests start with the masthead sloop as an arbitrary upper limit on headsail size with the catboat, no headsail, as a lower limit. Table 6-1 lists the planform geometries for the configurations tested. The configuration code of interest are

```
Tl5MH1 -- Full hoist masthead sloop
Tl5781 -- 7/8 hoist headsail sloop
Tl5341 -- 3/4 hoist headsail sloop
Tl5121 -- 1/2 hoist headsail sloop
T0SCB1 -- catboat (no headsail)
```

A general "bareboat" test was made to determine the lift, drag and vertical force of the hull and standing rigging. This force data has been reduced to coefficient form using the area of each respective configuration and they have been subtracted from the total configuration coefficients to give an estimate of the force and moment contribution made by the

sails alone. Since drag associated with boundary layer interactions between the sails and rigging are present in the full configuration tests, such estimates for the sails alone are considered to be conservative.

Figure 8-6 through 8-15 give the results of these tests. In each case, both the "complete rig" and the "sails alone" data are displayed. As such, it is evident that the hull and rigging not only contribute to drag but to lift as well. Therefore, conclusions directed towards over-all on-the-water boat performance could be misleading, if only the aerodynamics of the sail were considered. [The same observation was made earlier by Marchaj (8).] On a macro scale the maximum value of the lift coefficient appears to increase monotonically as the jib hoist is decreased. This is seen to be true for both the complete rig and the sails alone. However, the drag is also seen to increase proportionately, somewhat offsetting the useful component of lift. This will be discussed in greater detail in Chapter Nine.

High Aspect Ratio Series

A 150% masthead sloop configuration is chosen to assess the effects that different rig aspect ratios have on the resultant sail forces and moments. Configuration Reference codes T15MJ2 and T15MJ3, given in Table 6-1, were tested for comparison with the standard sloop T15MH1. The results of these tests are given in Figures 8-16 through 8-19 and are discussed in the next chapter.

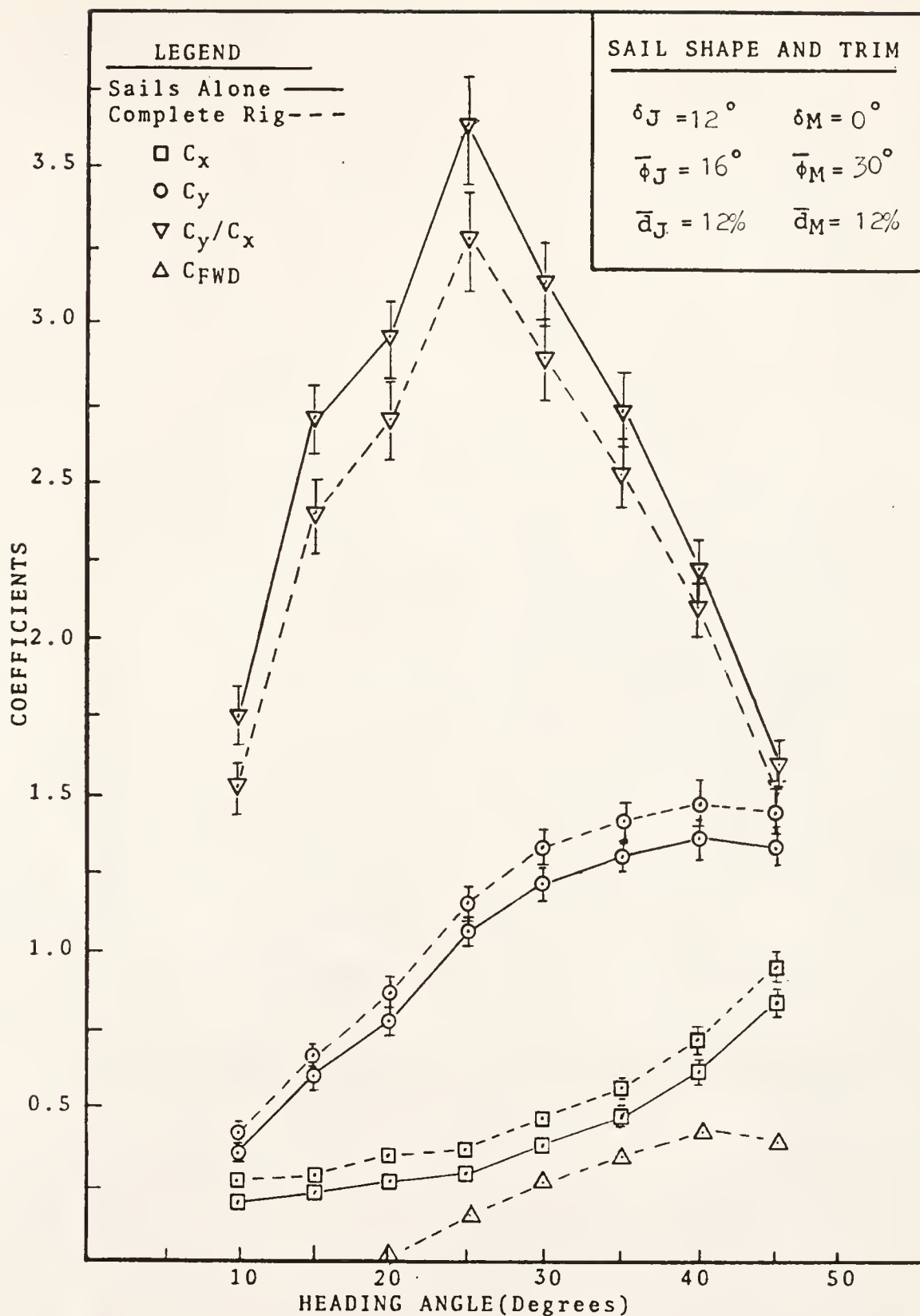


FIGURE 8-6 Masthead Sloop Test Results. $R_e = 280,000.$,
 $FBA/SA = 0.23$ (T15MH1)

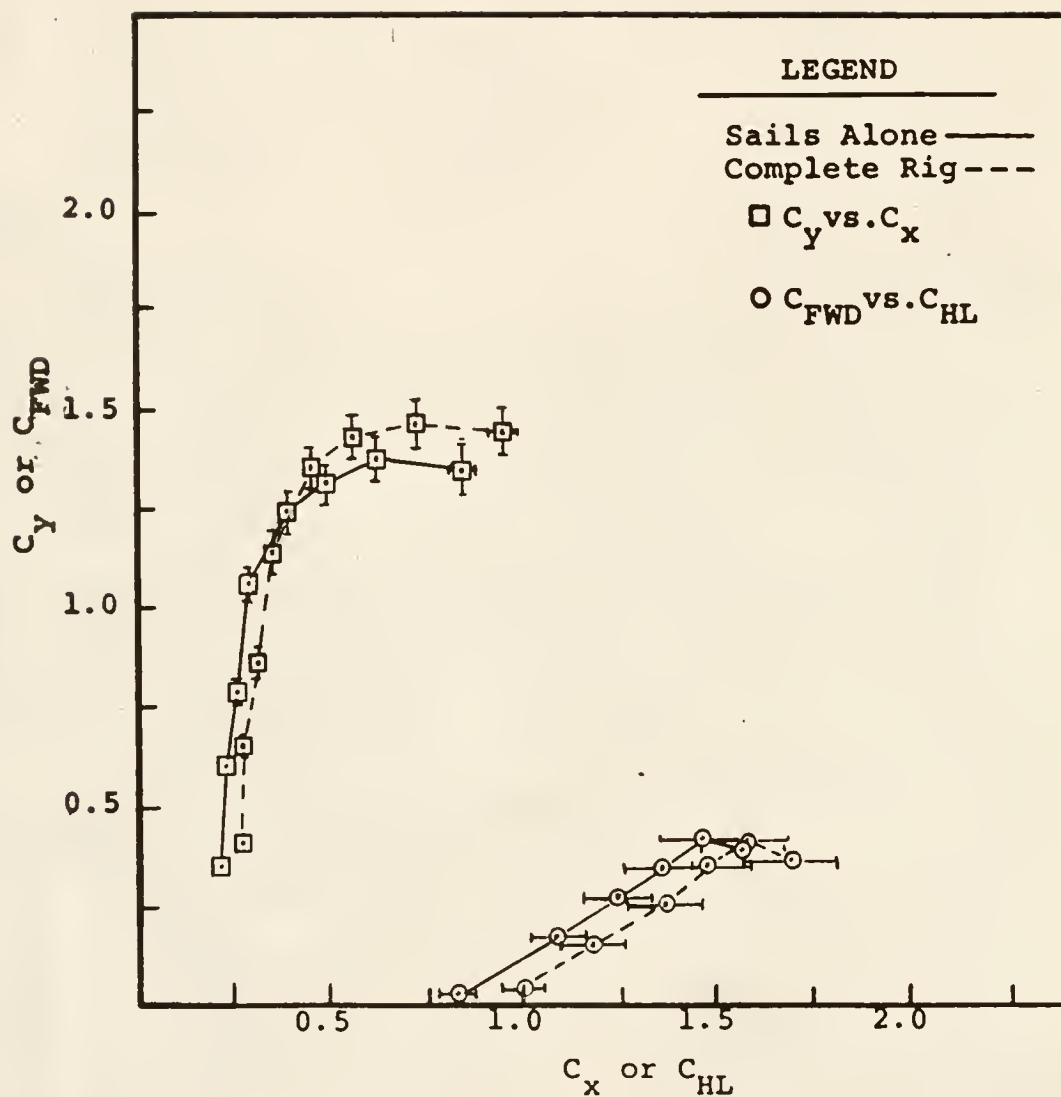


FIGURE 8-7 Masthead Sloop Polars. $Re = 280,000.$,
 $FBA/SA = 0.23$ (T15MH1)

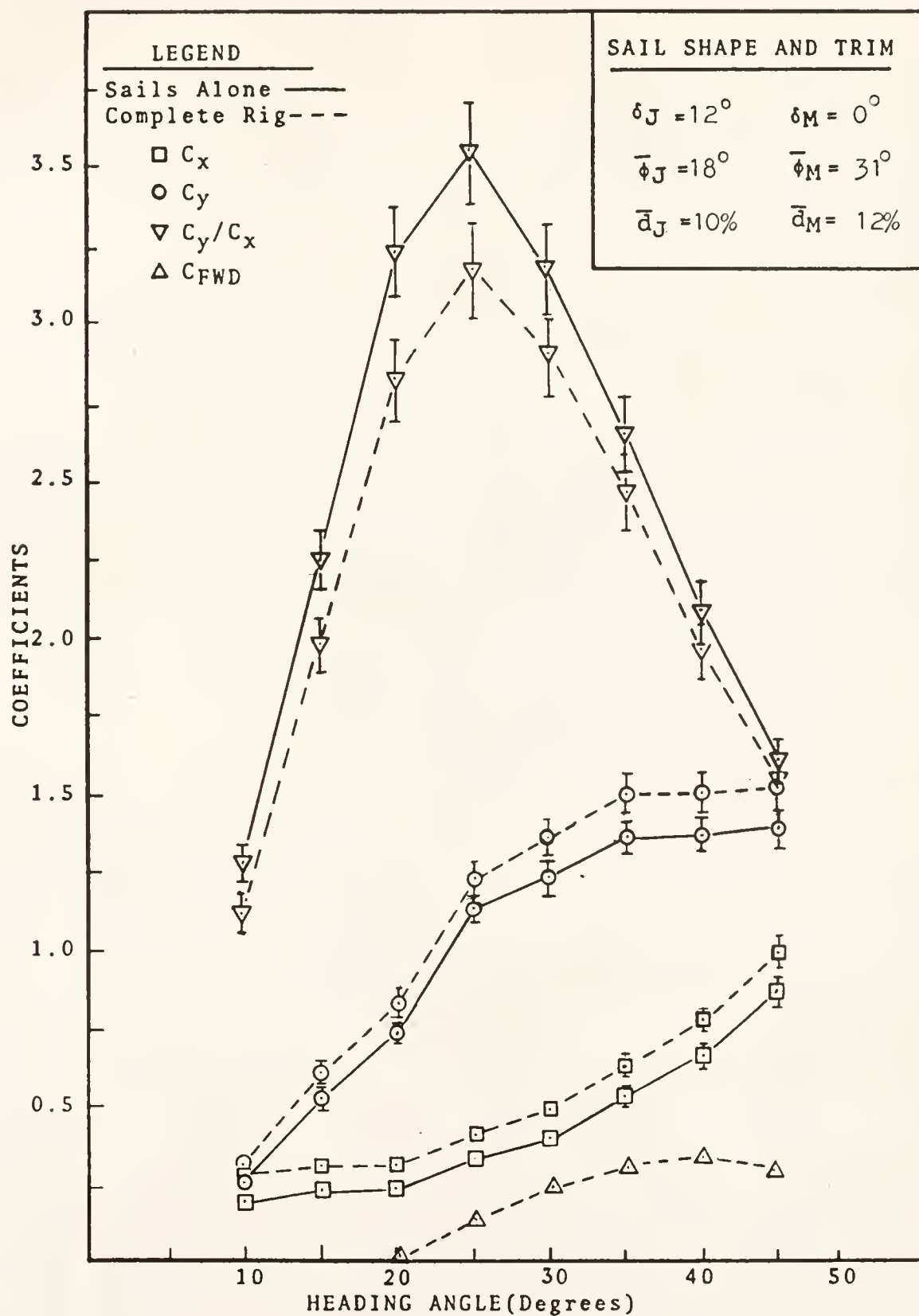


FIGURE 8-8 7/8 Sloop Test Results. $Re = 240,000.$,
 $FBA/SA = 0.27$ (T15781)

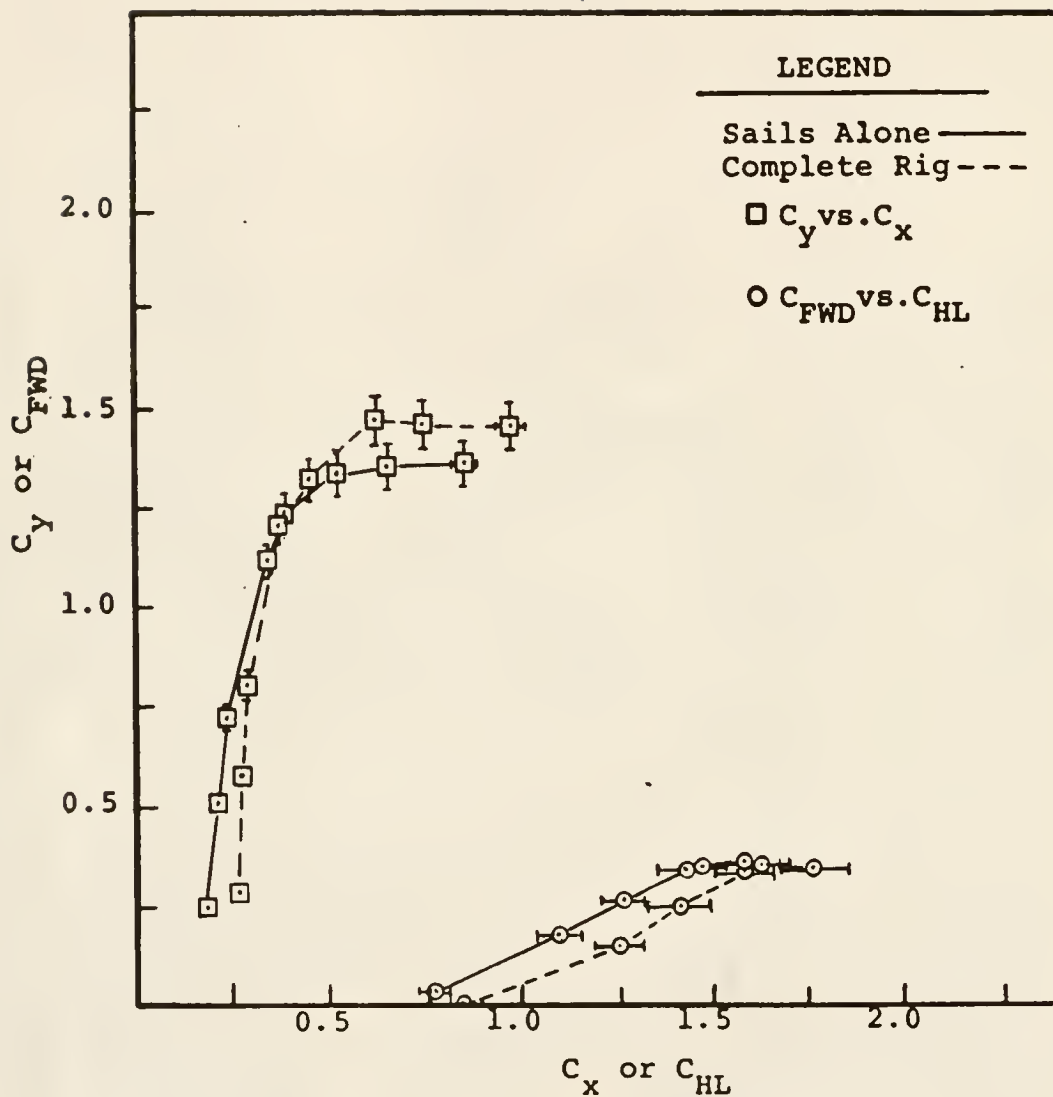


FIGURE 8-9 7/8 Sloop Polars. $Re = 240,000$. ,
 $FBA/SA = 0.27$ (T15781)

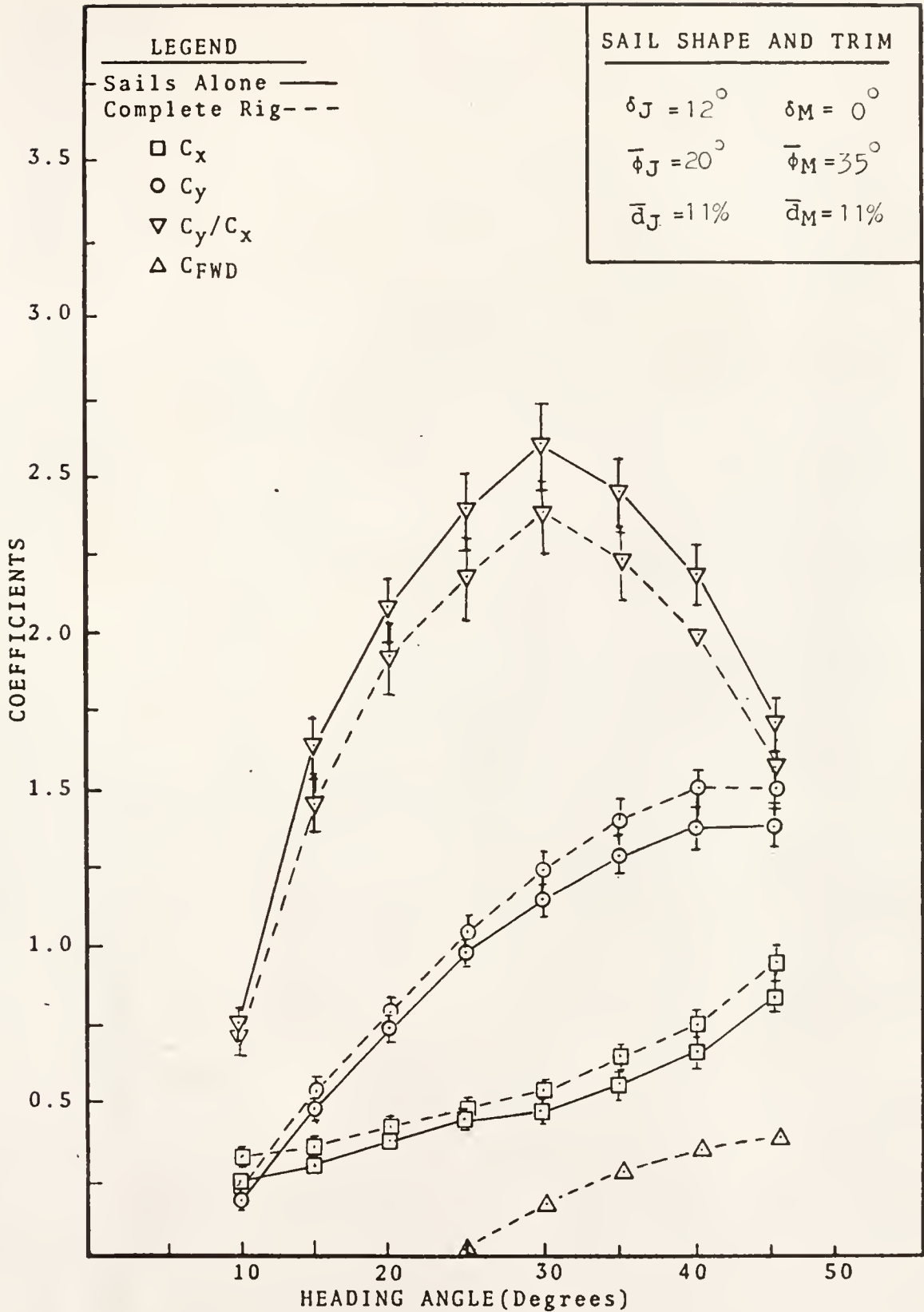


FIGURE 8-10 3/4 Sloop Test Results. $Re = 210,000$. ,
 $FBA/SA = 0.32$ (T15341)

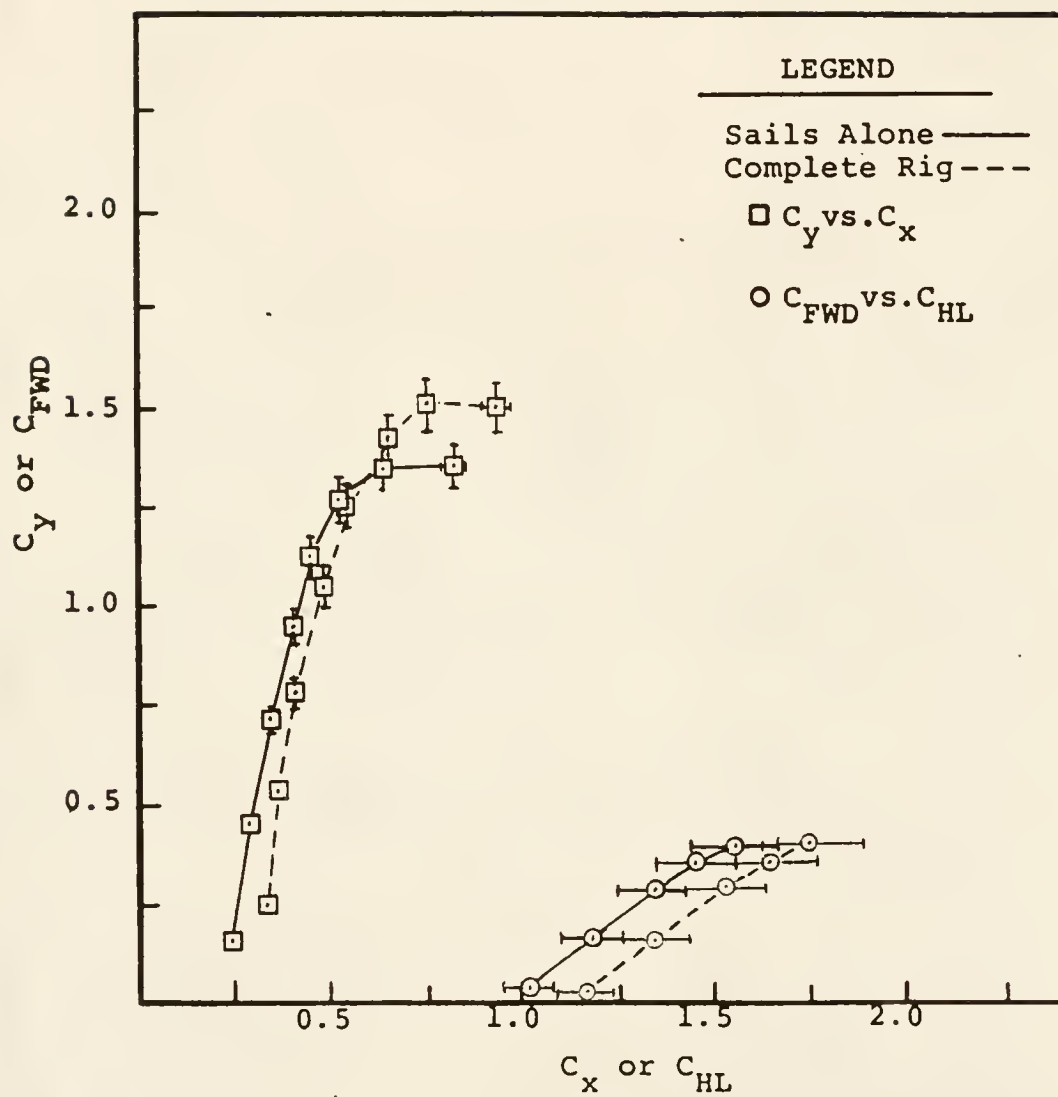


FIGURE 8-11 3/4 Sloop Polars. $Re = 210,000$. ,
FBA/SA = 0.32 (T15341)

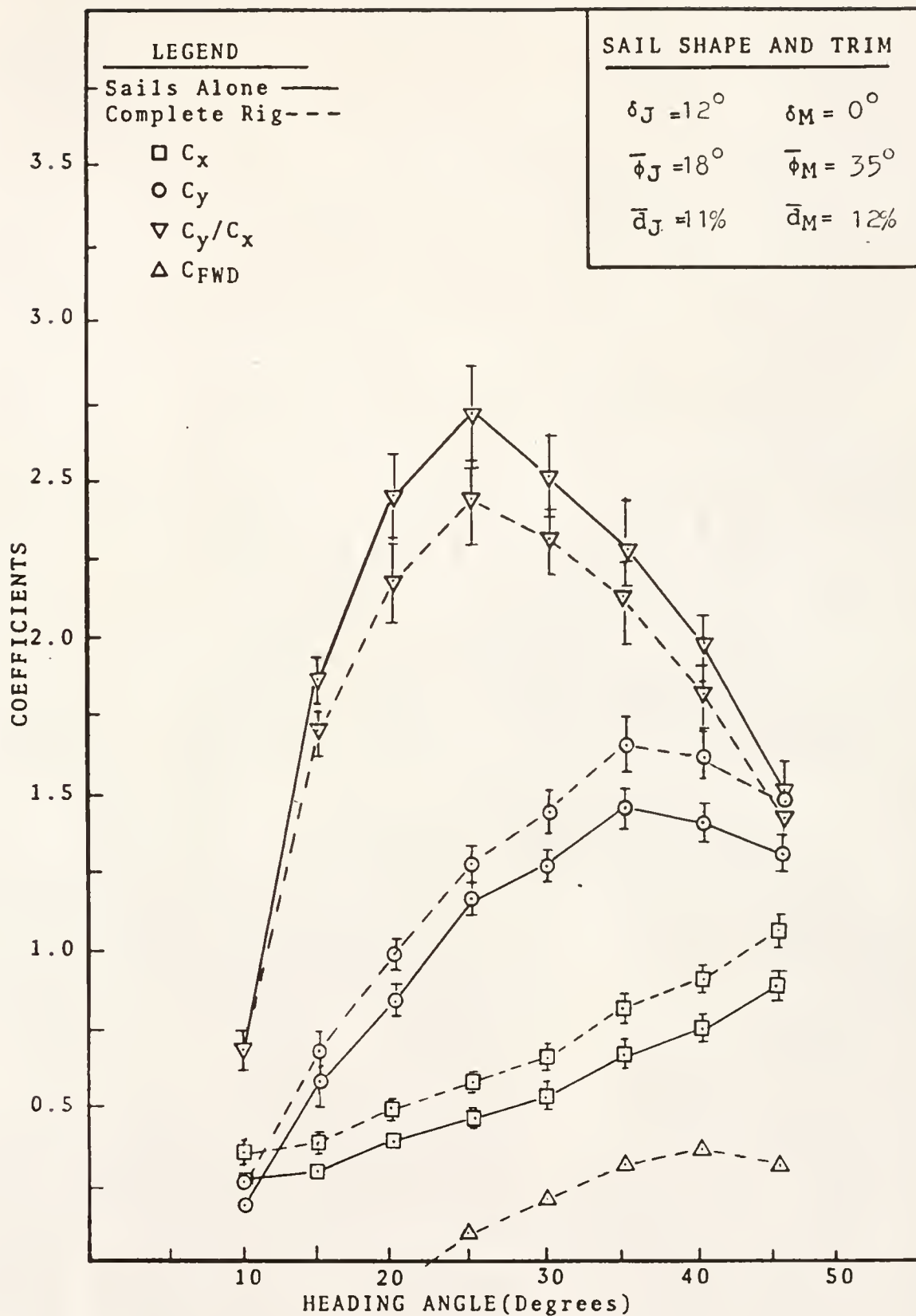


FIGURE 8-12 1/2 Sloop Test Results. $Re = 90,000$. ,
 $FBA/SA = 0.43$ (T15121)

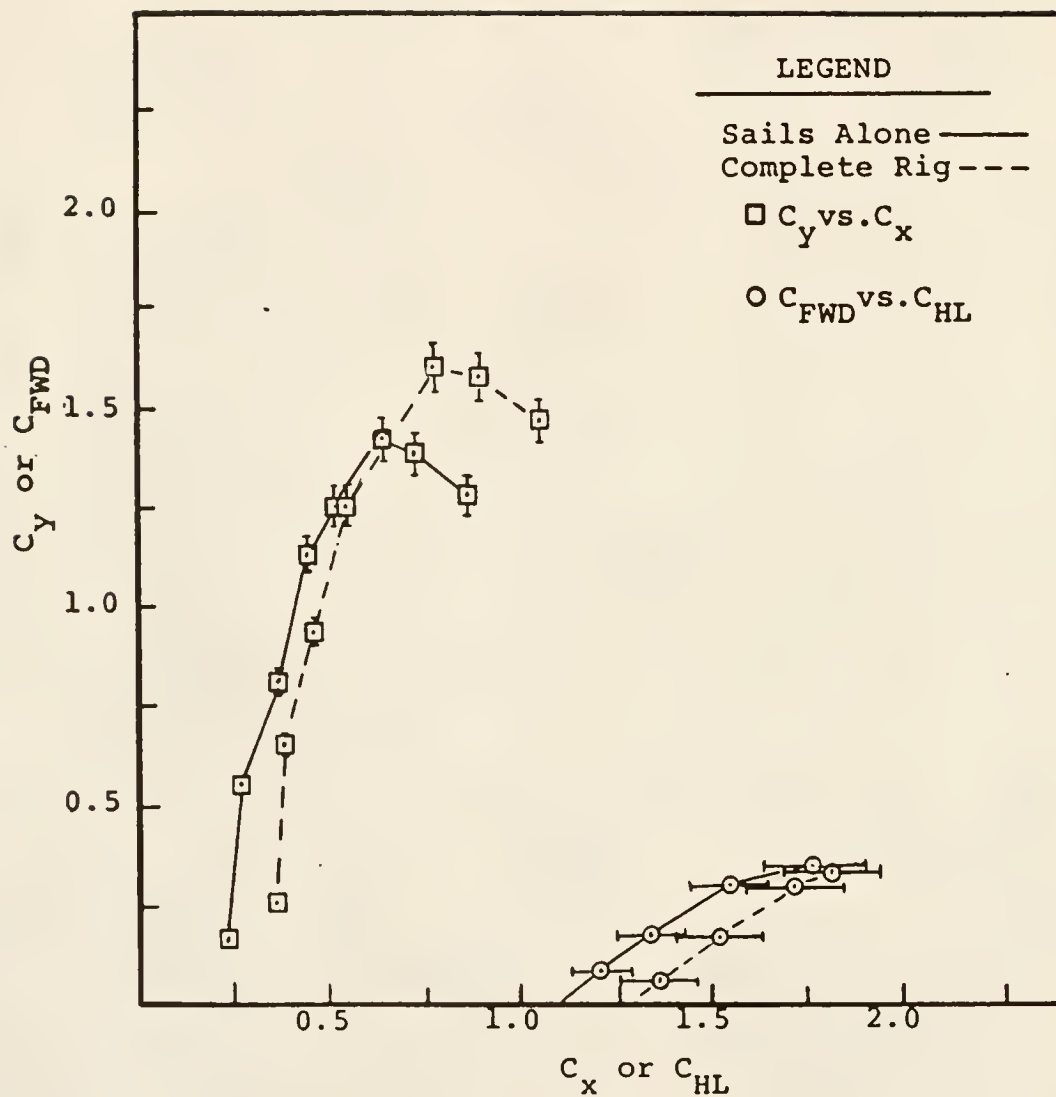


FIGURE 8-13 1/2 Sloop Polars. $Re = 90,000$. ,
 $FBA/SA = 0.43$ (T15121)

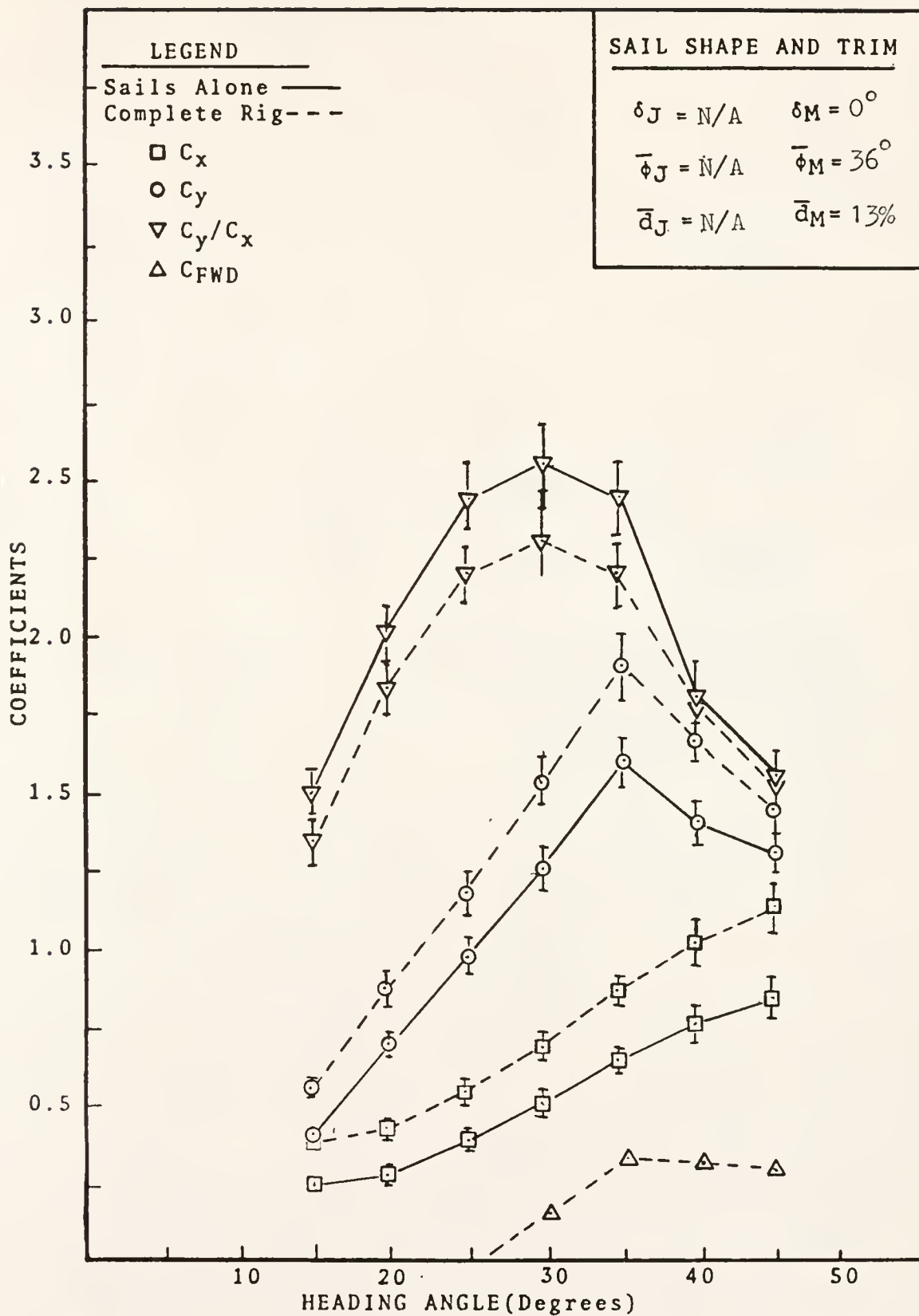


FIGURE 8-14 Catboat (with hull) Test Results. $R_e = 90,000$.
 $FBA/SA = 0.60$ (TOSCB1)

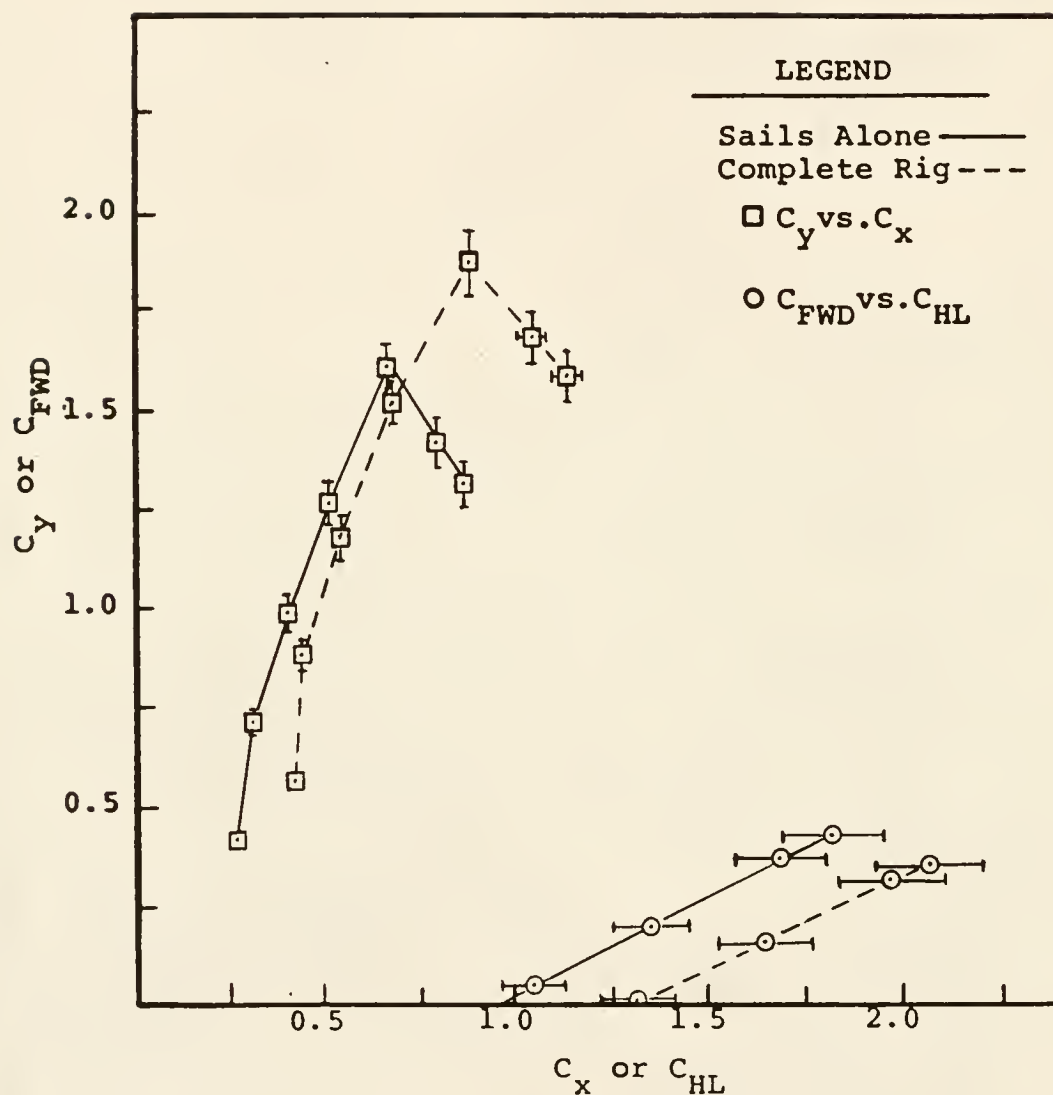


FIGURE 8-15 Catboat Polars (with hull).
 $R_e = 90,000$, FBA/SA = 0.60 (TOSCB1)

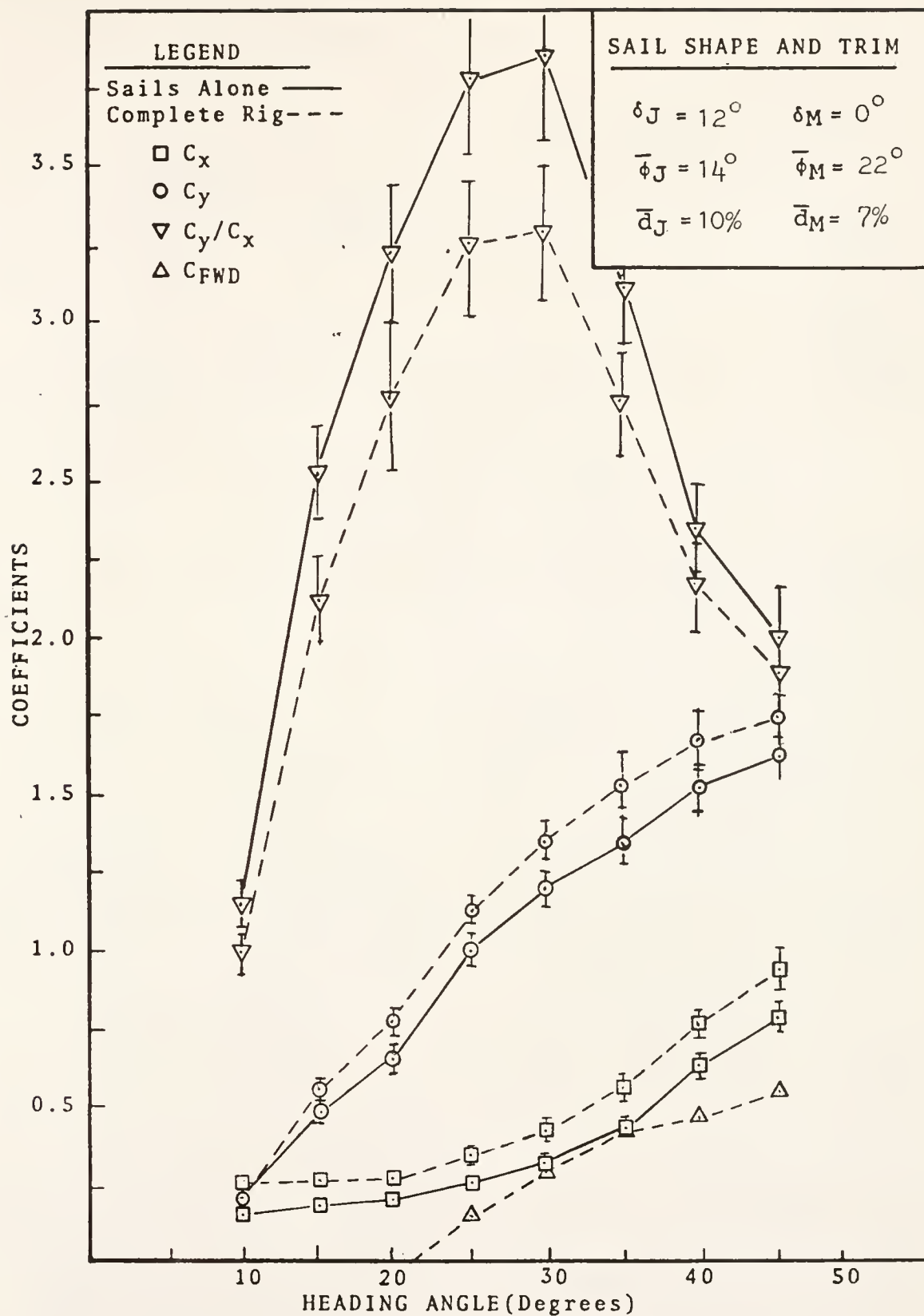


FIGURE 8-16 Masthead Sloop, High Aspect Ratio Main and Jib. $R_e = 260,000$. , $FBA/SA = 0.32$ (T15MJ2)

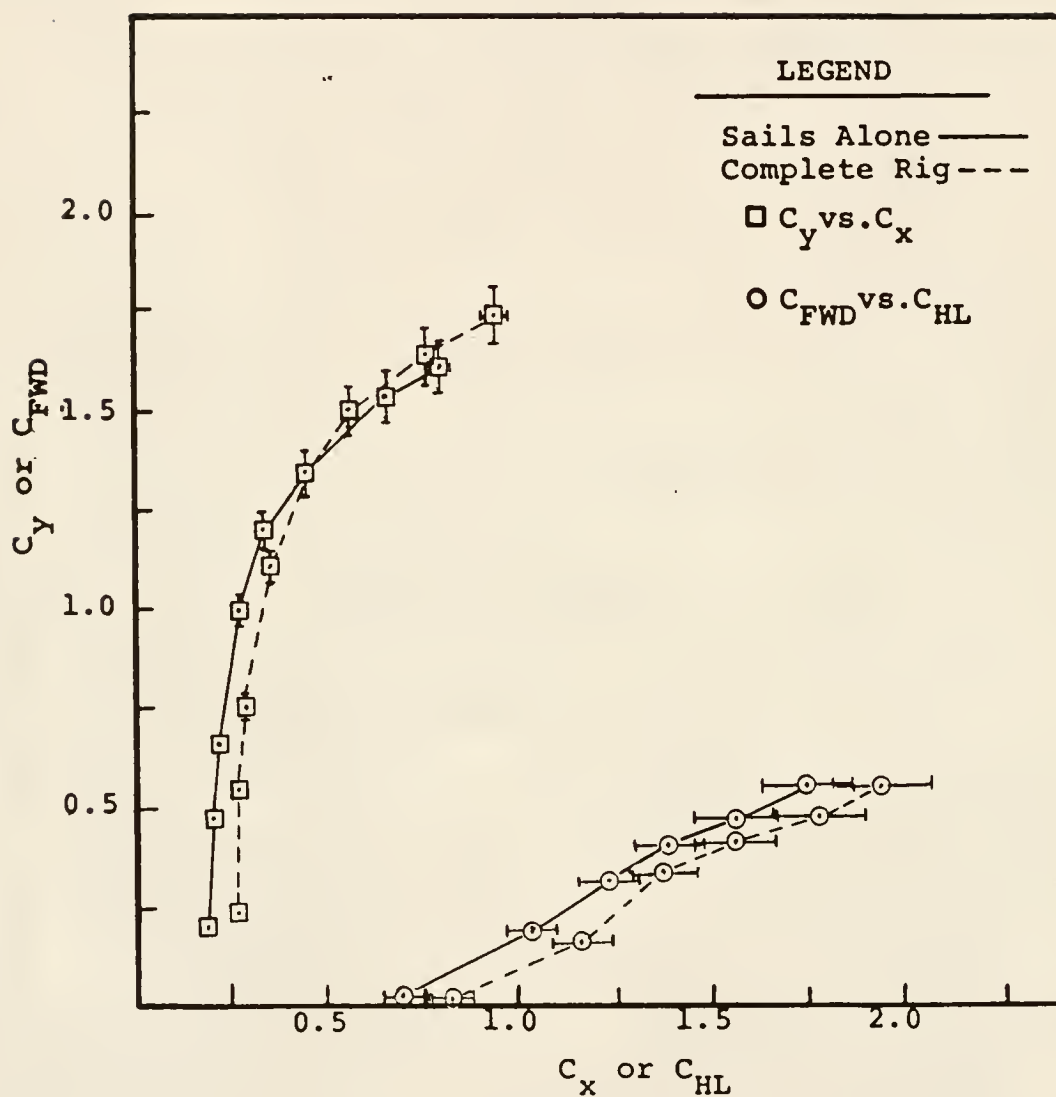


FIGURE 8-17 Masthead Sloop, High Aspect Ratio
Main and Jib, Polars. $Re = 260,000$.
 $FBA/SA = 0.32$ (T15MJ2)

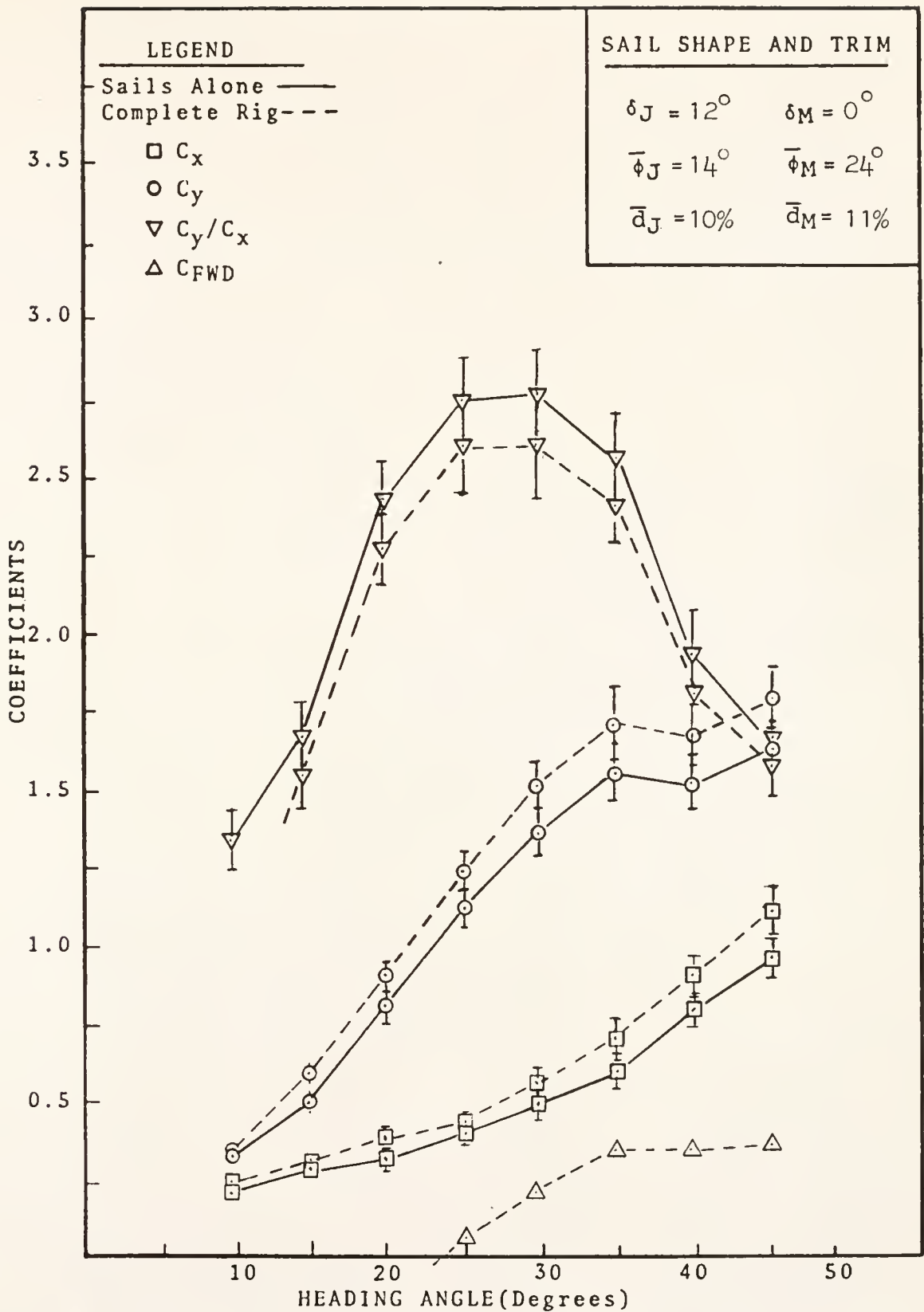


FIGURE 8-18 Masthead Sloop, High Aspect Ratio Jib and Standard Main. $Re = 270,000$. $FBA/SA = 0.28$ (T15MJ3)

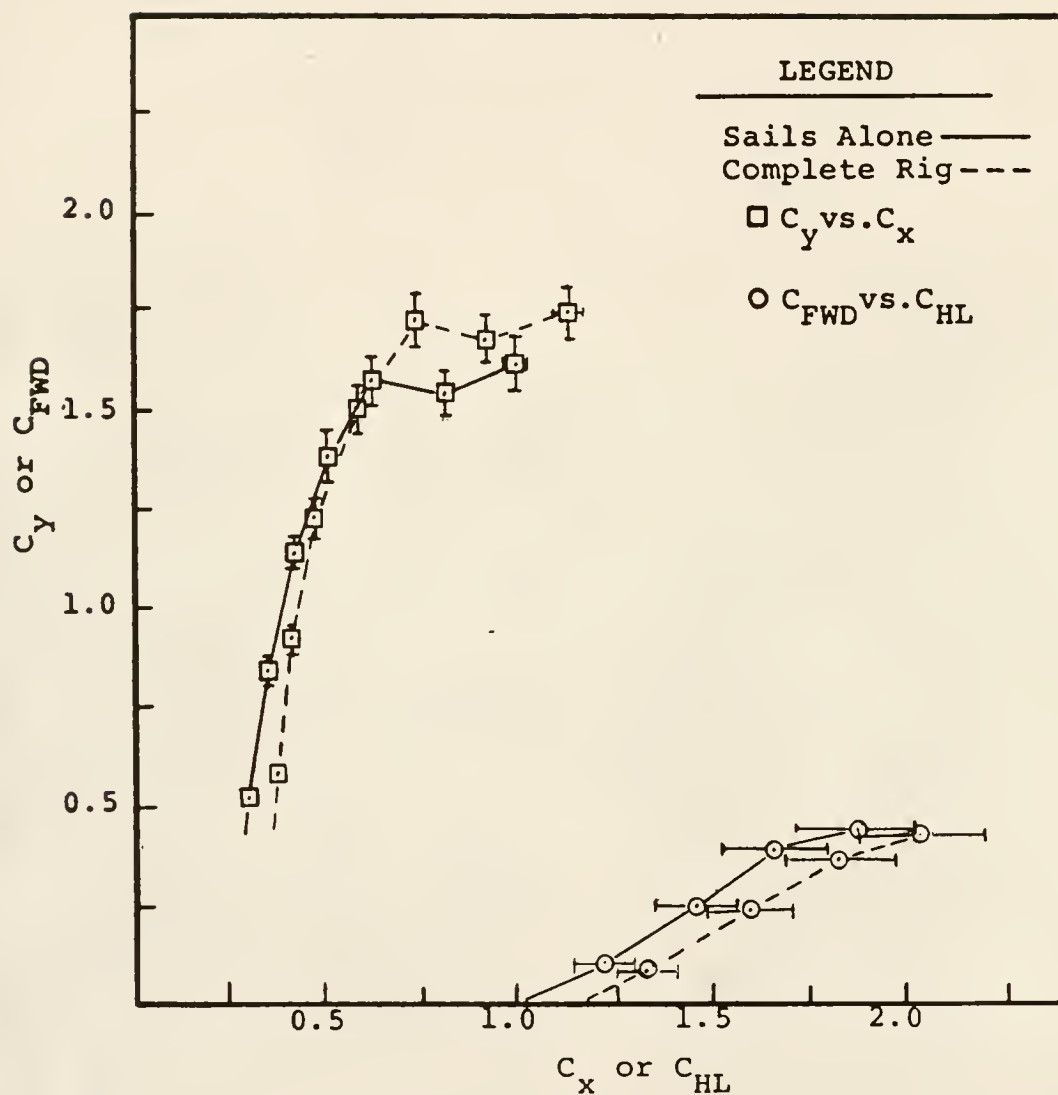


FIGURE 8-19 Masthead Sloop, High Aspect Ratio Jib, Polars. $R_e = 270,000$. , $FBA/SA = 0.28$ (T15MJ3)

CHAPTER NINE
EXPERIMENTAL AND THEORETICAL
COMPARISONS

The Influence of the Simulated Planetary Boundary Layer

Wind tunnel tests of yacht models made with and without a simulated PBL indicate that the influence of the PBL is significant, accounting for reductions on the order of 28% in the lift and drag coefficients relative to those without the PBL for the same trim and attack angles. In addition, the effective force center with the PBL is 30% farther above the deck than with no PBL model while yielding approximately the same moment coefficients about the water plane. This is important in that test work which does not model the PBL properly will tend to over predict yacht potential performance relative to lift and drag for the same vessel heel limitations. The assessment of the PBL onset profile by direct integration suggests that tests conducted with a uniform onset velocity model can be analytically corrected for any given onset velocity profile providing a functional formulation of that profile is possible and that some knowledge of the lift coefficient variation with height is available. Such a correction scheme could allow the test results of one model configuration to be applied to any size prototype independent of

similitude requirements for the PBL with all such requirements evaluated by analysis.

The direct impedance method offered for onset velocity profile synthesis is effective and practical, giving reasonable control over the velocity distribution and the turbulence in the wind tunnel test section. As pointed out earlier, the scale of turbulence for the test must be large relative to the turbulence scale of the sail model viscous shear layer. The evaluation provided here indicates that a reasonable PBL model should have approximately two orders of magnitude difference between these turbulence scales. For the prototype yacht, it can then be concluded that the scale of turbulence in the over-the-water PBL is insignificant and can be ignored relative to the sail aerodynamic performance.

A review of previous two and three-dimensional sail test data (8, 9, 10, 11, 13, 29) for lift and drag reveals a sensitivity to Reynolds number. This indicates a further complexity in a complete treatise on sail aerodynamics. Recall that in subsonic airfoil theory where the Reynolds numbers are large the viscous properties of flow may be neglected in the determination of lift and have only limited impact on drag. In contrast, experimental evidence for sails suggests that both lift and drag are strongly affected by their much lower Reynolds numbers at all angles of attack. It is speculated here that this Reynolds number sensitivity

is closely related to boundary layer stability. Specifically, the sharp edges of the single camber sail are simultaneously subject to an adverse pressure gradient and a fairly large free stream turbulence intensity; both of these effects could cause boundary layer instability. More research dealing with flow separation on sails at Reynolds numbers of 5×10^5 and less is needed.

Comparison of Potential Flow Models and Wind Tunnel Results

A potential flow analysis of the sail rig configurations described in Chapter Six has been made employing Register's SAIL-3 computer program. SAIL-3 produces numerical solutions to the classical potential flow problem of the Neumann exterior type by means of a discrete vortex lattice distribution. Register has shown SAIL-3 to be convergent to the exact solution of the governing equations in terms of detailed wake geometries as well as force coefficients with upper bound uncertainties for lift coefficients of $\pm 6\%$ and $\pm 17\%$ for drag coefficients for the specific sail discretization reported. This same discretization is used here. Therefore, the same uncertainty estimates are applicable. Rig configuration T15MH1 has been evaluated using SAIL-3. The results are given in Figure 9-1. Upon comparing these calculated results with the wind tunnel test results for the "sails alone" (see Figure 8-6) remarkable agreement is observed in lift up to the region of sail stall ($\beta=35^\circ$). In this same region, the drag coefficient values are seen to be under predicted. This is to be expected with an inviscid solution.

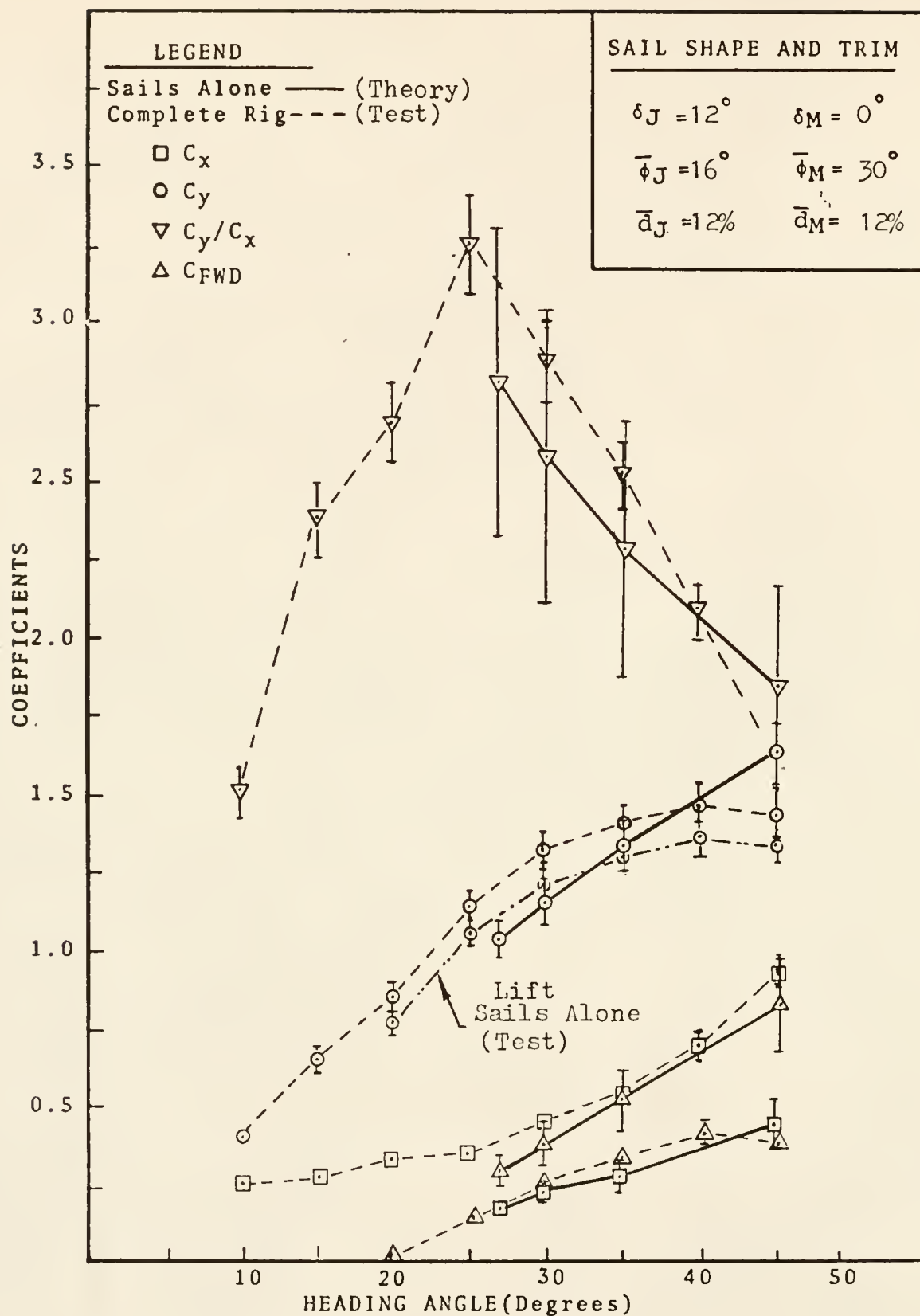


FIGURE 9-1 Masthead Sloop Theoretical C_x , C_y , C_{FWD} , and C_y/C_x vs. Test Results.
(ref. T15MH1)

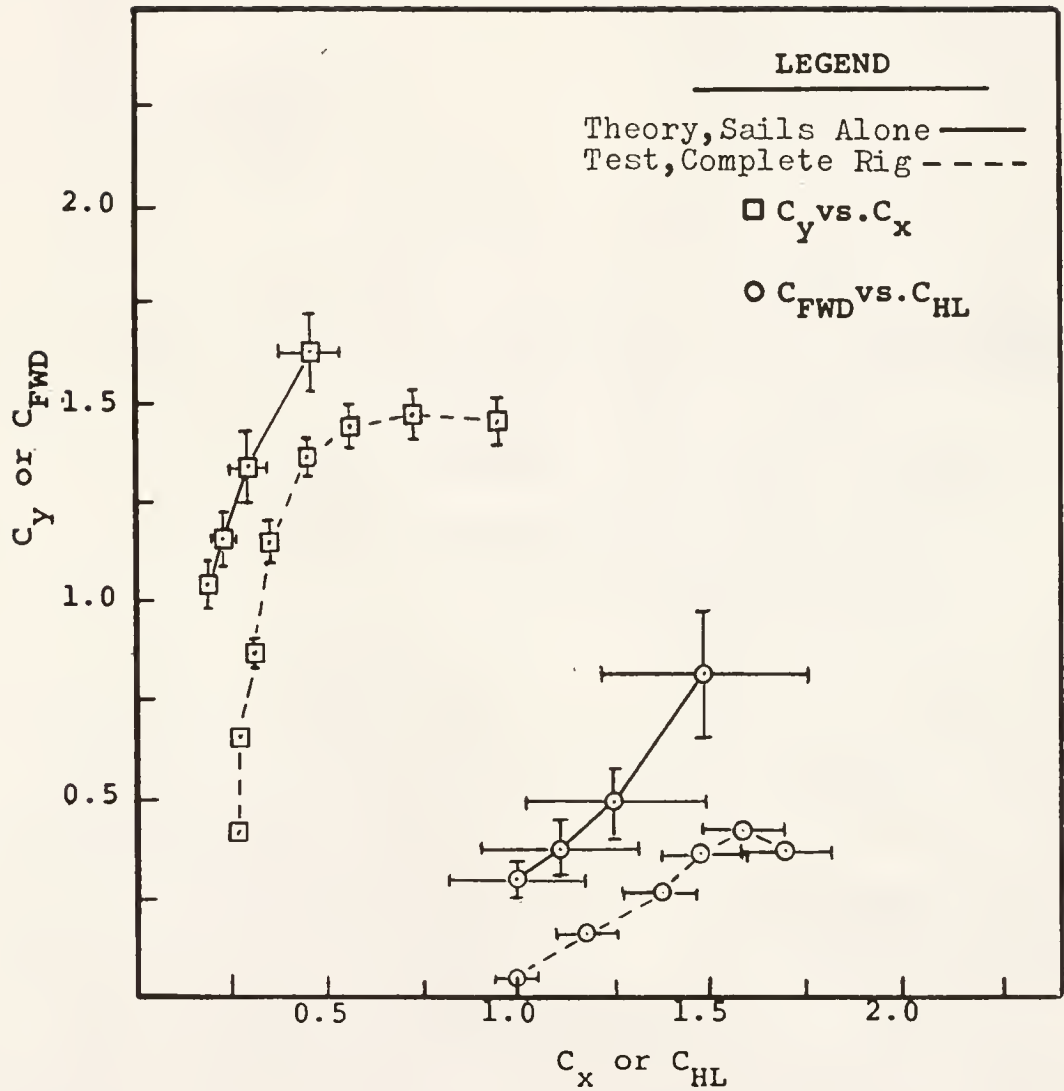


FIGURE 9-2 Masthead Sloop Theoretical Polars vs. Test Polars. (ref. T15MH1)

In that light, the results appear to agree in trend. The following configuration comparisons are limited to a single heading. This permits comparison of the present experimental values with the theory of Register (7) but also with that of Milgram (30). Table 9-1 gives a comparison of each configuration code listed in Table 6-1 all at a heading angle of 27° , a jib trim angle of 12° and a main-sail trim angle of approximately 0° . These trim selections are matched with the calculations reported by Milgram and Register. Test values are selected from the appropriate test results of Chapter Eight for a heading angle of 27° (Figure 8-6 through 8-19). It should be noted that the values indicated as SAIL-3 have been calculated for conditions that model the wind tunnel test conditions. That is, the rig dimensions and sail twist were set equal to those of the test prototype and the SAIL-3 boat speed was set equal to zero. Hence, SAIL-3 provides a theoretical analog of the wind tunnel test prototype.

Milgram systematically analyzes sail force coefficients (30) for two distinct sailing conditions which are termed the "high lift" and "reduced lift" conditions. The high lift condition corresponds to a sail lift distribution optimized for maximum drive force coefficients. This is analogous to the light wind sailing state that is simulated in the wind tunnel tests. Therefore, these results have been selected for comparison with the test values, Table 9-1. Milgram's coefficients, however, reflect a boat speed equal to 60% of

TABLE 9-1
Rig Configuration Test and Theoretical Results

Description		Aspect Ratio			Rig Ratios		Sails Alone					Complete Rig		
		Rig $H^2/2SA$	Main P/E	Jib 1/J	1/P	J/E	C_y	C_x	C_{FWD}	C_{HL}	C_y/C_x	C_y	C_x	L/D
Masthead Sloop T15MH1	(1)	1.14	2.50	3.13	1.11	0.89	1.04	0.18	0.46	1.01	5.62			
	(2)	1.50	2.50	3.00	1.10	0.89	1.25	0.26	0.45	1.24	4.73			
	(3)	1.50	2.50	3.00	1.10	0.89	1.25	0.22	0.51	1.21	5.83			
	(4)	1.14	2.50	3.13	1.11	0.89	1.15	0.32	0.20	1.33	3.56	1.24	0.40	3.10
7/8 Sloop T15781	(1)	1.40	2.50	3.13	0.97	0.78	0.95	0.15	0.24	0.91	6.40			
	(2)	1.82	3.00	2.78	1.02	1.10	1.24	0.26	0.32	1.22	4.80			
	(3)	1.82	3.00	2.87	1.02	1.10	1.24	0.21	0.36	1.20	5.96			
	(4)	1.40	2.50	3.13	0.97	0.78	1.16	0.35	0.12	1.18	3.31	1.30	0.41	3.17
3/4 Sloop T15341	(1)	1.63	2.50	3.13	0.83	0.67	0.91	0.13	0.29	0.87	6.85			
	(2)	2.04	3.00	2.63	0.96	1.10	1.24	0.26	0.33	1.22	4.68			
	(3)	2.04	3.00	2.63	0.96	1.10	1.24	0.02	0.37	1.20	5.77			
	(4)	1.63	2.50	3.13	0.83	0.67	1.02	0.41	0.10	1.10	2.49	1.15	0.50	2.30
1/2 Sloop T15121	(1)	2.21	2.50	3.13	0.56	0.44	0.91	0.14	0.24	0.78	5.86			
	(2)	2.58	3.00	2.25	0.82	1.10	1.24	0.28	0.32	1.23	4.49			
	(3)	2.58	3.00	2.25	0.82	1.10	1.24	0.22	0.36	1.20	5.54			
	(4)	2.21	2.50	3.13	0.56	0.44	1.18	0.46	0.12	1.35	2.57	1.34	0.57	2.35
Catboat T0SCB1	(1)	2.50	2.50				1.44	0.21	0.46	1.37	6.77			
	(2)	2.50	2.50				1.42	0.22	0.45	1.35	6.56			
	(3)	2.50	2.50	N/A	N/A	N/A	1.42	0.21	0.51	1.33	9.57			
	(4)	2.50	2.50				1.48	0.53	0.20	2.00	2.97	1.72	0.70	2.46
High AR J&M Masthead Sloop T15MJ2	(1)	1.65	3.50	2.78	1.11	0.95	1.03	0.13	0.35	0.97	8.14			
	(2)	1.67	3.50	2.75	1.10	1.41	1.23	0.25	0.34	1.21	4.92			
	(3)	1.67	3.50	2.75	1.10	1.41	1.03	0.20	0.38	1.19	6.13			
	(4)	1.65	3.50	2.78	1.11	2.95	1.15	0.37	0.20	1.25	3.11	1.28	0.47	2.72
High AR Jib Masthead Sloop T15MJ3	(1)	1.44	2.50	2.78	1.11	0.67	1.09	0.18	0.34	1.05	6.19			
	(2)	1.44	2.50	2.75	1.10	1.01	1.24	0.28	0.32	1.23	4.50			
	(3)	1.44	2.50	2.75	1.10	1.01	1.24	0.23	0.36	1.21	5.47			
	(4)	1.44	2.50	2.78	1.11	0.67	1.25	0.45	0.20	1.38	2.78	1.38	0.50	2.76

Legend:

- (1) Sail-3 results.
- (2) Milgram's results with a pressure and viscous drag estimate.
- (3) Milgram's results without the pressure and viscous drag estimate.
- (4) Wind tunnel test results.

the apparent wind speed. There are also minor differences in rig dimensions, as noted. Nevertheless, comparison remains of interest. To facilitate this comparison, when necessary, Milgram's results have been interpolated or extrapolated from neighboring cases to rig dimensions that are equivalent to those of the the wind tunnel tests.

Additional comparisons between theory and test results are shown in Tables 9-2 and 9-3. These compare the test results for the Finn model to a SAIL-3 analysis of a similar configuration and extrapolated catboat data from Milgram. In this case, comparison of the heights to the force center is possible, Table 9-2. These heights have been normalized using the hoist of the mainsail (P). It is evident that agreement between methods is good.

Next consider the drag coefficients. SAIL-3 only models induced drag. Milgram's vortex line model similarly yields only induced drag but his report also includes an independent estimate of viscous and pressure drag for the sail and mast. Comparing the drag coefficient entries of Table 9-2, it is clear that the addition of a pressure and viscous drag estimate to the potential flow prediction of induced drag gives drag coefficients close to the test results. It is also clear from the listed lift coefficients that Milgram has made no adjustment to the calculated lift as a consequence of the estimated pressure and viscous drag.

TABLE 9-2
Finn-Type Sail Results From
Experiment and Theory*

	Coefficients				
	C_y Lift	C_x Drag	C_{FWD} Forward	CHL Heel	z/p Side Force
TEST (T00CB5)	1.050	0.280	0.230	1.130	0.52
SAIL-3 (After Register)	1.258	0.232	0.424	1.196	0.518
MILGRAM (With Viscous and Pressure Drag Extrapolated)	1.405	0.271	0.410	1.372	0.522
MILGRAM (No Viscous and Pressure Drag Extrapolated)	1.405	0.208	0.450	1.350	0.529

* $\beta - \lambda = 27^\circ$

TABLE 9-3
Masthead Sloop (T15MH1) For Various β
Experiment vs. Theory

β	Source of Data	Coefficient	
		C_y	C_x
25°	(1) SAIL-3	0.96	0.14
	(2) MILGRAM	1.20	0.25
	(3) TEST	1.05	0.30
27°	(1) SAIL-3	1.05	0.18
	(2) MILGRAM	1.25	0.26
	(3) TEST	1.14	0.32
30°	(1) SAIL-3	1.16	0.23
	(2) MILGRAM	1.27	0.26
	(3) TEST	1.22	0.38

NOTE: ASPECT RATIOS

	<u>MAIN</u>	<u>JIB</u>
(1)	2.50	3.13
(2)	3.00	3.00
(3)	2.50	3.13

The need for such an adjustment can be illustrated considering the experimental values for lift and drag coefficients, Table 9-1. These data verify that when headsail size is reduced, the effect of viscous and pressure drag from mast and sail become increasingly important. This is well illustrated by the lift to drag ratio (L/D). This parameter is representative of potential rig performance. Figure 9-3 presents a plot of this ratio as a function of the ratio of jib hoist to mainsail luff (I/P). Appropriate values of I/P and L/D are selected from Table 9-1.

As noted in Chapter Eight, sail only results obtained by subtracting bareboat from complete rig measurements include the changes in lift and drag associated with the boundary layer interaction of the mast and sail. Therefore, the lift to drag ratio for these sail-only results provide insight as to the magnitude of the performance reduction that results from such boundary layer interaction. Figure 9-3 shows that the difference between the L/D calculated by Milgram with the viscous and pressure drag estimates and that given by the wind tunnel test decreases as I/P increases. This suggests that the mast is relatively more important with respect to the performance potential of the catboat than that of the masthead sloop. It is apparent that the inclusion of viscous and pressure drag in the potential flow analysis produces a trend in calculated L/D that more closely follows the wind tunnel test data. Without the inclusion of a drag model, potential flow theory (indicated by SAIL-3) and the

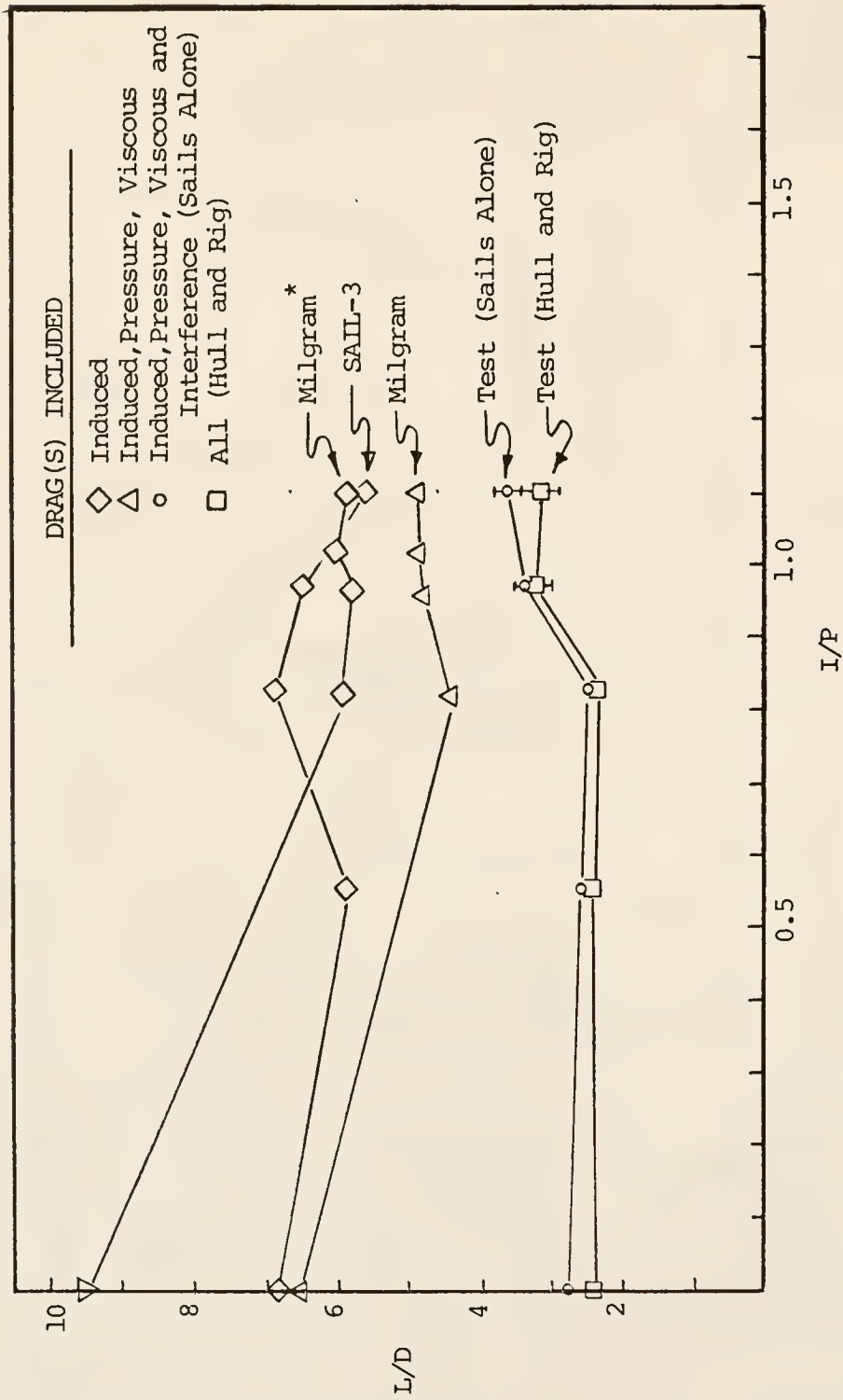


FIGURE 9-3 L/D Ratios From Theory and Test. Comparison of wind tunnel test data with potential flow theoretical predictions.

Milgram curve uncorrected is substantially above the data. The smallest value of L/D from test results occurs in the $3/4$ -fractional sloop. This minimum is thought to be caused by a combination of mast effect and the interactive headsail/mainsail induced flow. However, it should be noted that the magnitude of this minimum is only slightly less than the difference in measurement uncertainty. A relative maximum appears for the $7/8$ -fractional sloop. The differential between this maximum and the aforementioned maximum is greater than the measurement uncertainty.

CHAPTER TEN CONCLUSION AND RECOMMENDATIONS

Wind Tunnel Test Conclusions

The test results are sensitive to rig configuration and trim conditions. Experimentally predicted performance potentials agree with current sailing wisdom. The experimental uncertainties for the results presented are of the same order as those provided by the latest numerical techniques for potential flow models.

Comparison between the test results and the numerical potential flow models shows reasonable to good agreement for lift below rig stall. Above the stall point, the lift predicted by potential flow is incorrect. This affirms that stall inference is necessary to extend the analytical tools for a complete evaluation of yacht aerodynamic performance. Moreover, the important drive component of the total aerodynamic force produced by the sail(s) is given by the difference in the projections of lift and drag as indicated by equation 2-7. Therefore, a realistic drag value that includes viscous, pressure and parasite drag must be used to accurately predict rig aerodynamic performance potential. Since potential flow, by definition, includes only induced drag, an empirically based extension appears in order. Milgram's addition of these drag components is sensible and, apparently, of the correct magnitude

as the corrected predictions show good agreement with test. However, the effect of local separation, especially the three-dimensional local separation in the region of the mast of the lift produced by the sails, is of great significance.

In two-dimensional water tunnel tests, Milgram (11) found that the addition of the mast to the sail model increased the pressure and viscous drag to the order of that expected for the induced drag in a typical three-dimensional sail. For the sharp leading edge sail, experiment and theory have shown that the maximum pressure coefficient is developed near the leading edge. The inclusion of a mast, at the leading edge increases form and viscous drag. As a consequence, it also decreases the net lift. Thus, calculated lift to drag ratios and driving force coefficients that do not account for the mast effect on lift and drag will tend to overestimate performance. Further, the overestimation of performance will be greater for single sail catboats than for sloops.¹

Recommendations

Having summarized what has been investigated, the following parameters have not yet been adequately defined.

¹Sloops generally have a sharp leading edge on their headsails and the mast is well aft in the induced flow field. As a result, the pressure distribution obtained on the headsail is nearly that predicted by a no mast theory and this dominates the net aerodynamic force produced by the entire yacht rig. Conversely, the single sail of the catboat has the mast as its leading edge and is adversely affected by the mast/sail boundary layer interference.

The movement of the sail force center as a function of heading angle, especially on the sloop rig, is in order to give a better understanding of yacht hull/sail force balance. Further investigation into the variation of sail force coefficients with Reynolds is also needed. Sensible stall inference schemes for lift are needed to advance potential flow models of sailing yachts. In addition, empirically based estimates of viscous, pressure and interference drag must be included to make such models more realistic for yacht performance predictions.

Finally, future research should be focused towards comparing theoretical and wind tunnel test results with full scale yacht tests. Many of today's theoretical and empirical yacht sail investigations conclude with a direct application to a full size yacht. Unfortunately, the actual on-the-water performance of such an application is often inadequately measured to be useful as a check against theory and test. Such data would be invaluable towards advancing the complete understanding of sailing yacht aerodynamics.

APPENDIX A
SAIL TWIST AS RELATED TO THE APPARENT WIND
AND THE INDUCED FLOW FIELD

The vector addition of the local true wind velocity and the boat velocity results in a change of apparent wind direction as a function of vertical position. The angular magnitude of this change from the deck of the boat to the masthead, for conditions analogous to those being simulated here, may be assessed as shown below.

The law of cosines may be applied to the relationship between the true wind speed $U_T(z)$, boat speed U_B , and the true wind angle, γ , to determine the apparent windspeed U_A .

$$U_A = [U_T^2(z) + U_B^2 - U_T(z) U_B \cos(180-\gamma)]^{\frac{1}{2}} \quad (A-1)$$

Similarly the apparent wind angle, β , may be defined using the law of sines as

$$\beta = \arcsin \left[\frac{U_T(z)}{U_A} \sin(180-\gamma) \right] \quad (A-2)$$

These relationships describe the wind triangle for the sailing yacht (see Figure A-1). For this triangle it is convenient to parametrically define boat speed as directly proportional to the true wind speed at the masthead or

$$U_B = K_O U_T(H) \quad (A-3)$$

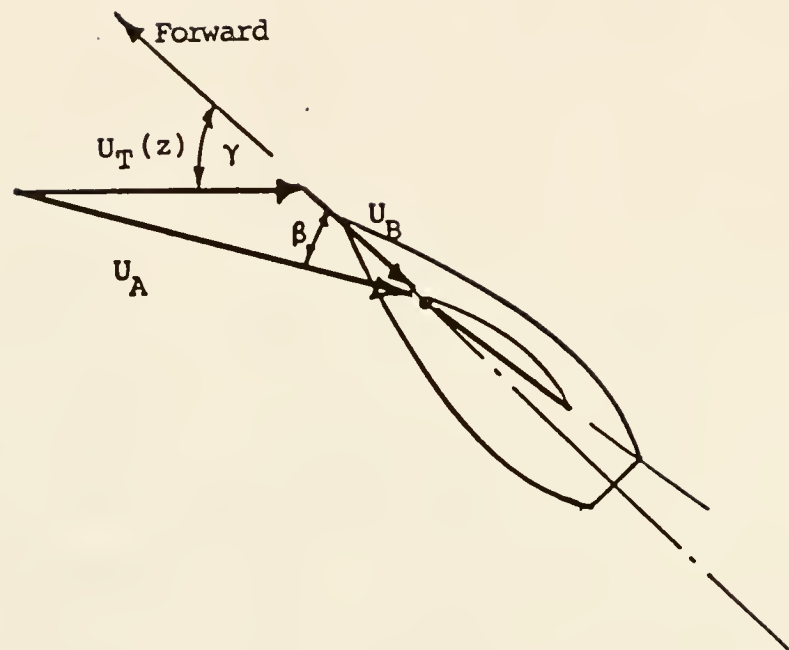


FIGURE A-1 Typical Wind Triangle for the Sailing Yacht

where K_O is the constant of proportionality. From Chapter Five the true wind speed at any height, z , may also be characterized in terms of the masthead wind speed as

$$U_T(z) = U_T(H) (A \ln z + B) \quad (A-4)$$

where A and B are constants (see Appendix D).

Substituting equations A-3 and A-4 into equations A-1 and A-2 and combining gives the apparent wind angle as a function of z alone for any selected K_O and γ as

$$\beta = \arcsin \left[\frac{\frac{\sin \gamma}{2}}{\left[\left(1 + \frac{K_O}{A \ln(z) + B} \right)^2 + 2 \frac{K_O}{A \ln(z) + B} \cos \gamma \right]^{\frac{1}{2}}} \right] \quad (A-5)$$

For the present analysis consider $U_T(H)$ to be U_{30} (the wind speed at 30 ft), $A = 0.1086$, $B = 0.4918$ and $K_O = 0.6$ (after Milgram). As a specific example let $\gamma = 45^\circ$ and then β can be calculated for the heights $z_1 = 3$ ft and $z_2 = 30$ ft; roughly the scale distance from the water to the yacht deck and from the water to the mast head respectively. Substituting into equation A-5 the change in the apparent wind angle is calculated as

$$\Delta\beta = \beta(z_2) - \beta(z_1) \quad (A-6)$$

From equation A-6 for $z_2/z_1 = 10$, $\Delta\beta$ from the deck to the masthead is calculated to be 7° . This change may be considered distributed over the span as an amount of "twist" in

the onset velocity profile. Comparing this typical wind twist to the twist of a well trimmed sail, one finds a difference on the order of 10° . That is, conventional sail trim (1, 2, 8) would call for a sail twist of approximately 20° for the conditions sighted. Obviously this twist would be excessive if it were to account for the wind/boat velocity addition alone. As per the flow observations discussed in Chapter Eight, a large induced upwash deflection of the wind field was observed at the sail leading edge. Good sail trim, in the form of a vertical distribution of twist, would require that the slope of the sail at the leading edge be parallel to the direction of the near field induced flow. That is proper trim avoids luff or stall at the leading edge.

Register (7) noted a need for a large amount of sail twist in order to maintain sensible pressure coefficients as predicted by his vortex lattice method. He found that twist of the order of 30° per span was required. This is in agreement with reasonable sail trim wisdom and in contrast to the 7° apparent wind twist calculated above.

Assuming that the induced field upwash angle is of the same magnitude as that of the induced downwash angle, one may provide a closure for this discussion. Adapting the method of Milgram (5), the local flow induced downwash angle (α_i) can be approximate in terms of the local induced downwash velocity $w(z)$ and the freestream velocity $U_T(z)$ as

$$\alpha_i = - \frac{w(z)}{U_T(z)} \quad . \quad (A-5)$$

With a linear approximation of the onset velocity profile, Milgram has shown that the induced downwash velocity may be described in terms of Fourier coefficients and the wind velocity at the sail mid-span as

$$w(z) = -U_o \sum_{n=1}^k n A_n (AA_n - BB_n)$$

where

- U_o = wind velocity at the sail mid span
 A_n = Fourier coefficients
 AA_n and BB_n = numerical solution points of the Glauert integral and the image sail integral respectively.

From Milgram's solution the values of AA_n and BB_n for $k = 3$ gives a representative downwash velocity at the boom and at the masthead, respectively, of

$$\begin{aligned} w(z_1) &= -0.189 U_o \\ w(z_2) &= -1.096 U_o \end{aligned} \tag{A-6}$$

The freestream velocity at these same locations may be approximated using Milgram's linear model

$$U_T(z_1) = U_o - 0.115(z_2 - z_1) \tag{A-7}$$

and

$$U_T(z_2) = U_o + 0.115(z_2 - z_1)$$

Selecting $U_o = 6$ ft/s as representative of the test conditions of interest, equations A-6 and A-7 can be evaluated

and substituted into equation A-5. The difference of the resulting induced attack angle from the boom to the masthead thus found is approximately 20° .

Conclusion

For the work reported here the similarity distortion of zero boat speed equates to a reduced sail twist on the order of 7° over that of the prototype. Roughly a 20% reduction in sail twist. (This twist is in good agreement with that which was determined during the observation tests as described in Chapter Eight). The significance of this twist reduction is that the forward components of lift will be slightly reduced while the aft component of drag is slightly increased over the entire sail planform. Both effects are thought to be conservative relative the net driving force and the potential yacht performance predictions that are suggested.

APPENDIX B
AERODYNAMIC FORCES AND THE
CENTER OF EFFORT

The sail is conveniently described in terms of curvilinear surface coordinates defined by the surface unit normal vector (e_1) and the unit tangent vectors (e_2, e_3) of two orthogonal surface parameter curves. Every point thus defined also has a description in a predetermined global cartesian coordinate system denoted by the position vector ζ_i . The net result of all the applied aerodynamic loads per unit area at any such surface point can therefore be considered as an applied surface traction ($\bar{T}$) described in the surface coordinates as a vector

$$T_j = T_1 e_1 + T_2 e_2 + T_3 e_3$$

The corresponding elemental differential surface area vector (ds_j) at this location has a magnitude (ds) defined as

$$ds = e_j ds_j$$

such that the differential force applied to this elemental surface can be written

$$df_j = T_j ds; \quad j = 1, 2, 3 \quad (B-1)$$

Equation B-1 represents a vector described in local coordinates. Let A_{ij} be the direction cosines between the local e-coordinates and a global coordinate system. Then A_{ij} is a function of the surface parameters and the differential force in the global coordinates becomes

$$df_i = A_{ij} T_j ds; \quad i = 1, 2, 3 \quad (B-2)$$

and the total force applied over all of the free surfaces is given in global form as

$$f_i = \iint_S A_{ij} T_j ds; \quad \begin{matrix} i = 1, 2, 3 \\ j = 1, 2, 3 \end{matrix} \quad (B-3)$$

The moment about the global origin resulting from the applied local surface traction at ζ_i is defined as the vector product of the position vector and the element force df_i . It is convenient to introduce the permutation symbol ϵ_{ijk} such that:

$$\epsilon_{ijk} = 0 \text{ if } i, j, k \text{ do not form a permutation of } 1, 2, 3.$$

$$\epsilon_{ijk} = +1 \text{ if } i, j, k \text{ form an even permutation.}$$

$$\epsilon_{ijk} = -1 \text{ if } i, j, k \text{ form an odd permutation.}$$

The global differential moment due to the local applied stress vector can then be written as

$$dM_i = \epsilon_{ijk} \zeta_j df_k$$

From equation B-2

$$dM_i = \epsilon_{ijk} \zeta_i A_{kl} T_l ds \quad (B-4)$$

The total global moment vector can then be obtained by integrating equation B-4 over all the free surfaces exposed to air flow or

$$M_i = \iint_s \epsilon_{ijk} \zeta_i A_{kl} T_l ds \quad (B-5)$$

Equations B-3 and B-5 demonstrate that three components of force and three components of moment define the net reaction of all the applied aerodynamic loads with respect to a preselected global origin. As such, the direct measurement of orthogonal force and moment triples at any preselected location, on or off the yacht, would give the net aerodynamic reactions.

It is frequently desirable, for measurement purposes, to define the global origin on a body immersed in a flow so that equation B-5 is identically zero. (The center of pressure of a two-dimensional airfoil for example). Early in the development of classical airfoil theory it was recognized that such a point shifted locations as the attack angle was varied. Subsequently, for symmetric models, a second point (the aerodynamic center [a.c.]) was defined about which the pitching moment coefficient remained constant with changes in the angle of attack. When testing an unsymmetric three-dimensional model such as the sailing yacht, insight to a

similar aerodynamic center or "center of effort" cannot be obtained, in general, by measurement of orthogonal force and moment triples alone. As proof, let F_i be a force vector applied at a location in space denoted by the position vector ζ_i^* such that:

$$M_i = \epsilon_{ijk} \zeta_j^* F_k \quad (B-6)$$

From equation B-5 then

$$\epsilon_{ijk} \zeta_j^* F_k = \iint_S \epsilon_{ijk} \zeta_i A_{kl} T_l ds$$

For static equilibrium

$$F_k = f_i$$

Equation B-6 may be expanded to yield a set of simultaneous scalar equations in $\bar{M}$, $\bar{\zeta}$, and $\bar{F}$ of the form

$$\begin{aligned} \bar{M}_1 &= \bar{F}_3 \bar{\zeta}_2^* - \bar{F}_2 \bar{\zeta}_3^* \\ \bar{M}_2 &= -\bar{F}_3 \bar{\zeta}_1^* + \bar{F}_1 \bar{\zeta}_3 \\ \bar{M}_3 &= \bar{F}_2 \bar{\zeta}_1^* - \bar{F}_1 \bar{\zeta}_2^* \end{aligned} \quad (B-7)$$

Now, consider the vector equation

$$M_i = B_{ij} \zeta_j \quad (B-8)$$

where the coefficient matrix B_{ij} is singular skew-symmetric which, by definition, has no inverse and is subject to the following conditions:

$$B_{ij} = -B_{ji}; i \neq j$$

and

$$B_{ij} = 0; i=j$$

Expanding equation B-8 into its component form, applying the above conditions and multiplying out the right hand side yields three simultaneous scalar equations of the form

$$\begin{aligned}\bar{M}_1 &= B_{12}\bar{\zeta}_2^* - \bar{B}_{31}\bar{\zeta}_3^* \\ \bar{M}_2 &= -B_{12}\bar{\zeta}_1^* + B_{23}\bar{\zeta}_3^* \\ \bar{M}_3 &= B_{31}\bar{\zeta}_1^* - B_{23}\bar{\zeta}_2^*\end{aligned}\tag{B-9}$$

By comparison, equation B-9 is seen to be identical to equation B-7 for

$$\begin{aligned}F_1 &= B_{23} \\ F_2 &= B_{31} \\ F_3 &= B_{12}\end{aligned}\tag{B-10}$$

Therefore, equations B-7 may be thought of as a vector equation of the form

$$M_i = F_{ij}\zeta_j^*\tag{B-11}$$

where the numerical subscripts on the matrix F_{ij} are related to the F_k as indicated by equation B-10. The desired solutions are the components values of ζ_i^* or

$$\zeta_i^* = [F_{ij}]^{-1} M_j \quad (B-12)$$

The solution indicated in equation B-12 exists if and only if the inverse of F_{ij} exists. But, for equation B-11 to represent the moment components of equation B-7, F_{ij} must be singular skew-symmetric. As such, equation B-11 does not have a unique solution and the definition of a "force center" is arbitrary with the measurement of only three forces and three moments. Conversely, the equations for static equilibrium allow only three orthogonal force and moment triples to be uniquely defined. If one of the position vector components ζ_i^* is known from symmetry or can be estimated from some physical constraint of the experiment, a unique solution of equation B-11 results. For the simplest case where a sail alone is tested, absent of hull and major portions of the rigging, a reasonable center of effort can be found by designing the test model so as to eliminate ζ_3^* , ($\zeta_3^*=0$), in equation B-7 allowing position vector components ζ_1^* and ζ_2^* to be determined. Even this method is approximate, owing to the fact that portions of the model structure like the mast and boom contribute drag and diminish lift, separating the effective lift center from the drag center relative to the global reference frame.

APPENDIX C AERODYNAMIC FORCE MEASUREMENT

Six-component Force Balance

The most appropriate approach to aerodynamic force and moment measurement is dependent upon the type of experiment proposed. As such, the requirements for the force measurement mechanism or "force balance" vary with application and are discussed in detail by Pope and Harper (21).

As suggested in the previous appendix, the most general applied force/moment combination requires a measurement of six independent quantities. A six-component force balance is necessary for such measurements. Such balances can be of varied designs; individual balances should be developed to be effective relative to their intended use. A typical six-component or six-channel force balance in the most general case would display cross coupling on all six channels. Consequently, the calibration procedure for such a balance would be quite complex and time consuming to implement. Each single-channel reading would reflect all three force and moment components due to this coupling. The general form of the relationships for any one channel reading is

$$R_i = \left(\frac{\partial R_i}{\partial e_j} \right) \left(\gamma e_j \right); \quad \begin{array}{l} i = 1, 2, \dots, 6 \\ j = 1, 2, \dots, 6 \end{array} \quad (C-1)$$

where R_i is the net reading (load cell output, electrical resistance, force, etc.) for any one channel in terms of the γ_{ej} actual applied calibration load or moment on any one channel and the cross coupling influence coefficients $\partial R_i / \partial e_j$ of all six channels. The calibration equations for any net, single-channel, reaction is then obtained by inverting equation C-1 or

$$\gamma_{ej} = \left(\frac{\partial R_i}{\partial e_j} \right)^{-1} R_i \quad (C-2)$$

Equation C-2 implies that each calibration equation is formed by making six separate calibration runs, one for each channel, and recording all six channel reactions for each applied known load. All of the data thus acquired is then assembled to form the influence coefficient matrix and inverted to yield the final calibration equations. The experimental uncertainty ($w_{\gamma_{ej}}$) of any γ_{ej} reaction is therefore, dependent on all six R_i channel readings and each of their respective individual uncertainties. Employing the methods discussed by Holman (22) and assuming a uniform odds distribution, the single channel uncertainty, $w_{\gamma_{ei}}$, is then given in terms of the individual reading uncertainties, w_{Rk} as

$$w_{\gamma_{ei}} = \left[\sum_{k=1}^6 \left(\frac{\partial \gamma_{ei}}{\partial R_k} w_{Rk} \right)^2 \right]^{1/2} \quad (C-3)$$

If the coupling effects are strong and the uncertainty of all the readings are approximately the same, the final net

reaction uncertainty is then determined by any one typical channel uncertainty as

$$w_{\partial ei} = w_R \left[\sum_{k=1}^6 \left(\frac{\partial \gamma_{ei}}{\partial R_k} \right)^2 \right]^{\frac{1}{2}} \quad (C-4)$$

For the worst case, all the $\frac{\partial \gamma_{ei}}{\partial R_k}$ are considered of equal magnitude and equation C-4 reduces to

$$w_{\gamma e} = \sqrt{6} \left(w_R \frac{\partial \gamma}{\partial R} \right)$$

In this case, the required precision for any one channel reading can be predetermined as a function of the final desired outcome uncertainty. This outcome can be improved by decreasing (elimination is the optimum) the cross coupling terms of equation C-3 and/or by reducing the reading uncertainty, w_R .

The pyramidal balance is classically offered as a means whereby all of the $\frac{\partial \gamma_{ei}}{\partial R_k}$ coupling terms of equation C-4 approach zero except for $i=k$. Accordingly, the pyramidal balance provides one independent reaction reading for each applied force or moment. [Reference (21) presents a discussion on the design and function of the pyramidal force balance.] This is accomplished through precise machining and alignment of the components to establish a balance focal point that serves as the balance global origin. Such machining and alignment can only be achieved with great care and, as a consequence, at considerable cost. If the model to be tested is symmetric about one plane (such as the airplane

with respect to the fuselage) and it is mounted on the balance so that this plane passes through the balance focus, all the forces and moments measured are uniquely positioned with respect to the global origin. For the sailboat model however, there is no such symmetry plane. As a result, at least a portion of the advantage of such a balance is lost; however, the unique correspondence of one balance reaction reading to each one applied load or moment remains an attractive balance feature.

As suggested above, one of the controlling parameters for experimental measurements is the acceptable level of uncertainty. If more than one channel reading is required to establish a net reaction at a balance point, the uncertainty of that reading is increased since the net uncertainty can be assessed at the outset, then the design requirements for individual channels may be defined by a relation similar to equation C-1.

The present effort is not funded by outside sources. It is therefore, limited with respect to the capital acquisition of equipment. All of the hardware and materials that are used herein are provided by Embry-Riddle Aeronautical University or at the author's personal expense in the spirit of altruistic sail research. A typical commercially available, six-component pyramidal force balance, such as Aerolab Supply Company's Model PD-3-6, is beyond the available resources for this effort. Consequently, a six-component

floating beam balance has been designed, constructed, and is employed as the data acquisition tool.

Six-component Floating Beam Balance

The design specification for the beam balance was to provide six force component measurements within an acceptable, predetermined, level of uncertainty without incurring exorbitant costs or requiring exotic manufacturing techniques. To this end, each of six channels is reduced to a simple cantilever beam fitted with electrical resistance strain gauges. The beam cross sections are sized as a function of the largest anticipated loads so as to remain within the small strain assumptions of beam theory. Primary forces F_x and F_y are determined from the sum of two forces and the inclusion of a z-moment coupling coefficient. The principle moments, M_x and M_y , are then obtained from the forces, F_x and F_y , and their relative position to a preselected global origin. This origin is arbitrary and for this exercise is selected as the intersection of the balance vertical axis and the wind tunnel test section floor. The vertical z-axis force and moment are measured directly. Figure C-1 schematically depicts this balance concept. The floating beam establishes the vertical z-axis and is connected to orthogonal pairs of cantilever beams at A, C and B, E through ball end connecting rods. Vertical displacement of the floating beam is prevented by way of a cable held taut from (A) to (B) and fixed through the end of a fifth cantilever beam (H)

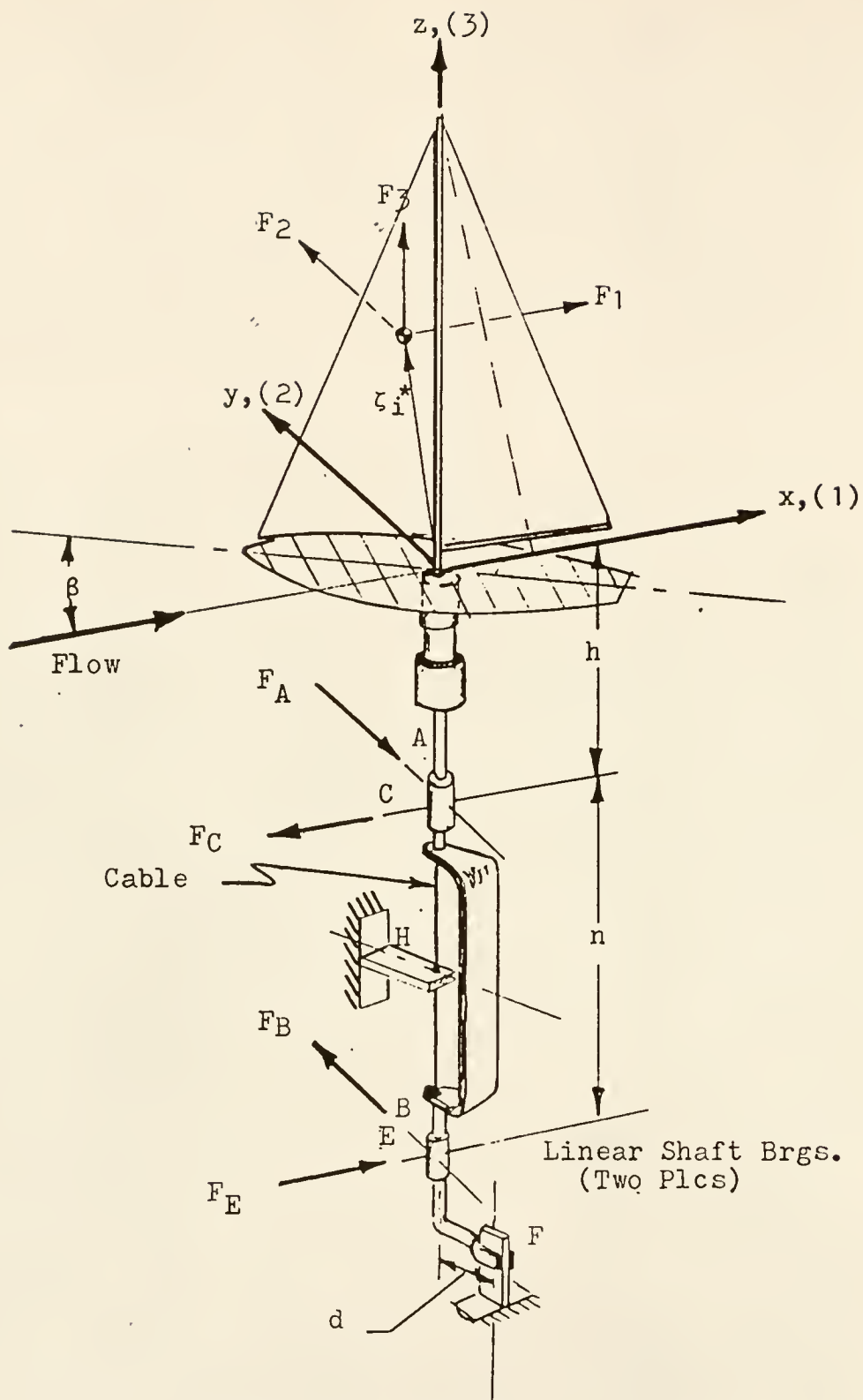


FIGURE C-1 Six Component Floating Beam Force Balance In Schematic.

which measures F_z directly. The moment about "z" is reacted at F by a sixth beam.

The subsequent force calibration equation takes the form

$$\begin{aligned} F_Y &= C_A R_1 + C_B R_3 + C_{AF} R_6 + C_{BF} R_6 \\ F_X &= C_B R_2 + C_E R_4 + C_{CF} R_6 + C_{EF} R_6 \\ F_Z &= C_H R_5 \end{aligned} \tag{C-5}$$

where C_A , C_B , etc. are calibration coefficients determined by application of known loads to each of the independent channels. Rearranging equation C-5 for the individual flexural element contributions gives

$$\begin{aligned} F_Y &= F_A + F_B \\ F_X &= F_C + F_B \\ F_Z &= C_H R_5 \end{aligned}$$

The principle moments are then functions of the known distances (h and n) and the measured forces or

$$\begin{aligned} M_X &= hF_A + (n + h)F_B \\ M_Y &= hF_C + (n + h)F_E \\ M_Z &= C_F R_6 \end{aligned} \tag{C-6}$$

Problems one would normally encounter with change in the model angle of attack or heading angle are avoided because the z-axis rotation does not effect "x" or "y"; that is both "x" and "y" are oriented with respect to wind tunnel flow direction independent of the heading angle. It is the heading angle that is varied throughout the testing. This is done manually while the wind tunnel is operating. The balance design allows the model to be rotated relative to the balance and then locked in place at a specific heading. Any small change in this angle due to displacements can be corrected by small angular displacements of the model relative to the wind tunnel floor (the x-y reference frame).

The experimental uncertainty for measurements made with this floating beam balance are assessed using equation C-5 and C-6 plus observing uncertainties from tests, manufacturer's calibration uncertainties for the instruments used and the calibration loading apparatus employed.

The calibration loads that were used are measure weights, accurate to 0.10 g. The loads were applied via cables over ball bearing pulleys. The pulley hysteresis and "stiction" were measured at $\pm 0.2\%$ of the applied load. Any single strain gauge channel could be statically read to an accuracy of $\pm 0.2\%$ of the scale of interest. The net accuracy of a calibration coefficient over a range of channel readings for all six channels used is adequately represented by an uncertainty of $\pm 1.1\%$. The useful output uncertainty of the balance for any one force or force

coefficient is seen, from equation C-5, to be dependent on as many as four such calibration coefficients. In addition, the output load (F_A , F_B , F_C , etc.) also contains from one to three separate channel readings with a larger uncertainty than for the static case due to vibrations, wind tunnel turbulence and model response to these excitations. For any such measured force, the principal quantity of interest is the force coefficient C_F defined as

$$C_F = \frac{F}{qA} \quad (C-7)$$

The uncertainty of q , the dynamic pressure, is dependent upon a water manometer reading, ± 0.010 in. of H_2O , and a temperature reading, $\pm 1^\circ F$. The area factor, A , is dependent upon actual model measurements. All model dimensions are accurate to within 0.032 in. The net uncertainty of a force coefficient due to dynamic pressure and area measurements is calculated to be $\pm 1.15\%$.

Each channel resistance, R_1 through R_6 , was assessed using the common uncertainties indicated above the equations C-5, C-6 and C-7. A uniform odds distribution was assumed and channel reading fluctuation histories were compiled from all of the test runs made. The experimental uncertainty for each channel was then statistically assembled over a range of coefficient values as a local mean of all the uncertainties in that range. This approach allowed the application of small uncertainties to the output readings near

stall when the model is highly loaded and the fluctuations in readings were small relative to the magnitude of the reading. Conversely, for zero loads or light loads, the reading uncertainties were larger and were reflected as such. It is the high loading cases which are of primary interest for the sailboat. From all the above considerations, the net uncertainty for each force coefficient range is reported in Chapter Three, Table 3-1.

APPENDIX D
WIND TUNNEL DATA REDUCTION
COMPUTER PROGRAM
"BOAT"

WIND TUNNEL TEST DATA REDUCTION PHASE

(* note *

The main program is written to be general
with the only arrays and/or
variables being stored in COMMON that are
applicable to any type of model that can
be tested in the wind tunnel.

The subroutines are geared
towards sailboat analysis.)

PART OF
WIND TUNNEL / DATA ACQUISITION UNIT / HP1000L PROJECT

J. LADESIC

ORIGINIALLY WRITTEN
26 MARCH 1982

MODIFIED
8 APRIL 1982
12 APRIL 1982
15 APRIL 1982
21 MAY 1982
9 MAY 1982
15 JUN 1982

```
COMMON      NR,ANGLE(30),V(30),RE(30),CX(30),CY(30),CZ(30),
/           CMX(30),CMY(30),CMZ(30),QA(30)
REAL MZ,JS,IS,EPSLN,AM,B2M,CM,EM,R6F
INTEGER FNAME(3),DESC(30)
DIMENSION CDR(30),CHL(30),CVT(30),CMHL(30),CMPT(30),CMYW(30)
/           BJ(30),BM(30)
DATA VK/.000157/
```

COMMON data table

NR : Number of wind tunnel runs for
model being tested
ANGLEj: Angle model is mounted relative to wind,
can be X - Y plane or X - Z plane
Vj : Airspeed
CXj,CYj,
CZj : Force coefficients X Y Z
coordinate system
CMXj,CMYj,
CMZj : Moment coefficients X Y Z
coordinate system
REj : Reynolds number
QAj : Dynamic force

WRITE (1,5)

```

5 FORMAT (" INPUT NAME OF FILE RUN DATA IS STORED IN" / 122
/ "(UP TO SIX CHARACTERS, FIRST CHARACTER START IN COLUMN 1,"
/ / " JUSTIFIED LEFT, INCLUDE TRAILING BLANKS, IF ANY)")

```

```

    READ (1,6) (FNAME(I),I=1,3)
6 FORMAT (3A2)
    OPEN (UNIT=6, IOSTAT=IERR, ERR=998, FILE=FNAME, STATUS='OLD')
    GOTO 50

```

```

C
998 WRITE (1,55) (FNAME(I),I=1,3), IERR
55 FORMAT (" ERROR ON OPENING DATA FILE ", 3A2
/ / " ERROR # ", I4)
    GOTO 999

```

```

C
50 WRITE (1,7)
7 FORMAT (/// 5X, "WIND TUNNEL DATA REDUCTION PROGRAM")
    READ (6,10) NR, (DESC(I), I=1,30)
10 FORMAT (I4, 30A2)
    WRITE (1,15)
15 FORMAT (////)
    ICHNRL=2
    CALL SINPT (IS, JS, P, ES, PC, FRF, AR, CHORD, SA, ARTD)

```

```

C
C*****KINEMATIC VISCOSITY CORRECTED FOR TEMP AS 1.083VK(STD)=VK
C
    CONST=CHORD/(1.083*VK*12.)

```

```

C
    DO 1 J=1, NR
    EPSLN=0.0
    READ (6,20) ANGLE(J), T, PS, PT, R1, R2, R3, R4, R5, R6, BJ(J), BM(J)
20 FORMAT (F5.1, F5.0, 2F7.2, 6F7.0, 2F5.1)
    A=((R1-4000)/(12.11))+((4000-R6)/307.656)
    B=((4000-R3)/(8.11))+((4000-R6)/1589.35)
    C=((R2-4000)/(83.3333))+((4000-R6)/300.0)
    E=((4000-R4)/(87.500))+((4000-R6)/4050.0)
    MZ=(4000-R6)/56.8627
    H=((R5+ (.276*R1)-5104)/161.8)
    QA(J)=(ABS(PT-PS)/12.)*62.4*SA
    EPSLN=1+.027*(SA+.45)*SIN(ANGLE(J)/57.3)
    QA(J)=(EPSLN**2)*QA(J)
    IF (QA(J)) 100, 200, 100
100 CX(J)=- (C+E)/QA(J)
    CY(J)=- (A+B)/QA(J)
    CZ(J)=H/QA(J)
    CMX(J)=((-4.625*A)-(16.063*B))/(QA(J)*CHORD)
    CMY(J)=((-4.625*C)-(16.063*E))/(QA(J)*CHORD)
    CMZ(J)=MZ/(QA(J)*CHORD)
    GOTO 300
200 CX(J)=0.
    CY(J)=0.
    CZ(J)=0.
    CMX(J)=0.
    CMY(J)=0.
    CMZ(J)=0.
300 V(J)=2.905*SQRT(ABS(PS-PT)*T)
    V(J)=EPSLN*V(J)
    RE(J)=V(J)*CONST
1 CONTINUE

```

```

C
    CALL SCDEF (CDR, CHL, CVT, CMHL, CMPT, CMYW)
    CALL SBOUT (IS, JS, P, ES, PO, FRF, AR, CHORD, SA, ARTD, CDR, CHL, CVT,

```

CLOSE (UNIT=6)

999 STOP

END

SUBROUTINE Sailboat INPUT

Accept sail dimensions data from CRT,
compute sail area (actual), sail area (100 % F.T.),
mean aerodynamic chord, aspect ratio

(* note *

subroutine is designed for sailboat applications,
however subroutine can serve as a model for
aircraft / missile / rocket type
applications)

SUBROUTINE WRITTEN
12 APRIL 1982

MODIFIED

9 MAY 1982 TO INCLUDE THE CATBOAT

```
COMMON NR,ANGLE(30),V(30),RE(30),CX(30),CY(30),CZ(30),
/      CMX(30),CMY(30),CMZ(30),QA(30)
REAL I,J,JSTAR
```

data table

```

I,J,P,E      : Sail(s) Dimensions
CHORD        : Mean Aerodynamic Chord of sail(s)
SA           : Total (Actual) Sail Area
ARTD         : Area of Sail(s) as projected to a plane
               parallel to longitudinal axis
AR           : Aspect Ratio of sail(s)
PO           : Percent Overlap
FRF          : Fractional Rig Factor

```

WRITE (1,5)

```
5 FORMAT (/ 5X,"SAIL BOAT DATA INPUT" //
```

/ " INPUT SAIL DIMENSIONS (for model) I J P E (inches)"

```
READ (1,*) I,J,P,E
```

WRITE (1,10)

```
10 FORMAT (/ " INPUT PERCENT OVERLAP, FRACTIONAL RIG FACTOR")
```

```
READ (1,*) PO,FRF
```

IF (FRF) 30,50,30

$$30 \text{ JSTAR} = \text{SQRT}((\text{PO}^{**}2)/((1./(J^{**}2)) - ((\text{PO}^{**}2) - 1.)/(I^{**}2)))$$
$$H = I / FRF$$
$$SA = (.5 * JSTAR * I + .5 * E * P) / 144.$$
$$ARTD = (.5 * J * I + .5 * E * P) / 144.$$

GOTO 60

$$50 \text{ ARTD} = .5 \cdot E \cdot P / 144.$$
$$H = P$$
$$SA = ARTD$$

60 AR = H**2/(SA*144.)

CHORD = SA*144./H

124

RETURN

END

SUBROUTINE SCDEF (CDR,CHL,CVT,CMHL,CMPT,CMYW)

SUBROUTINE Sailboat COEFFicients

Computes coefficients when rotating wind tunnel X - Y axis
to DRIVE - ATHWARTSHIP axis

SUBROUTINE WRITTEN

12 APRIL 1982

MODIFIED

(none)

data table

CDR, CHL, CVT : force coefficients

CMHL, CMPT,

CMYW : moment coefficients

COMMON NR,ANGLE(30),V(30),RE(30),CX(30),CY(30),CZ(30),

/ CMX(30),CMY(30),CMZ(30),QA(30)

DIMENSION CDR(30),CHL(30),CVT(30),CMHL(30),CMPT(30),CMYW(30)

DO 1 J = 1,NR

CDR(J) = CY(J)*SIN(ANGLE(J)/57.3)-CX(J)*COS(ANGLE(J)/57.3)

CHL(J) = CY(J)*COS(ANGLE(J)/57.3)+CX(J)*SIN(ANGLE(J)/57.3)

CVT(J) = CZ(J)

CMHL(J) = CMY(J)*SIN(ANGLE(J)/57.3)-CMX(J)*COS(ANGLE(J)/57.3)

CMPT(J) = CMY(J)*COS(ANGLE(J)/57.3)-CMX(J)*SIN(ANGLE(J)/57.3)

CMYW(J) = CMZ(J)

1 CONTINUE

RETURN

END

SUBROUTINE SBOU (I,J,P,E,PO,FRF,AR,CHORD,SA,ARTD,CDR,CHL,CV
/ CMHL,CMPT,CMYW,BJ,BM,IDES,FNAME)

SUBROUTINE SailBoat OUTput

SUBROUTINE WRITTEN

14 APRIL 1982

MODIFIED

(none)

COMMON NR,ANGLE(30),V(30),RE(30),CX(30),CY(30),CZ(30),

```

/      CMX(30),CMY(30),CMZ(30),QA(30)
REAL I,J
DIMENSION CDR(30),CHL(30),CVT(30),CMHL(30),CMPT(30),CMYW(30)
/      BJ(30),BM(30),IDES(30)

```

C

```

WRITE (1,5) (IDES(K),K=1,30),I,J,P,E
5  FORMAT (///// 5X,"WIND TUNNEL EXPERIMENT, DATA REDUCTION ",
/"OUTPUT" // " TEST MODEL :  SAIL BOAT" // 1X,30A2 ///
/ " SAIL DIMENSIONS : " / " I = ",F7.2,7X,"J = ",F7.2,7X,
/"P = ",F7.2,7X,"E = ",F7.2 )
WRITE (1,10) AR,CHORD,SA,ARTD,PO,FRF
10 FORMAT (5X," ASPECT RATIO = ",F7.2 / " MEAN AERODYNAMIC ",
/"CHORD = ",F7.2," in." / 5X," SAIL AREA (ACTUAL) = ",F7.2,"f
/ / 6X,"SAIL AREA (100 % F.T.) = ",F7.2 / 5X,"PERCENT OVERLA
/,F7.2 / 5X,"FRACTIONAL RIG FACTOR = ",F7.2)

```

C

```

WRITE (1,15)
15 FORMAT (/// " SAIL ANGLES(DEG),VELOCITY(FPS),REYNOLDS NUMBER
/"DYNAMIC FORCE(q*SA)LE)" //
/ " COURSE",1X,2(3X,"Delta"),18X,"REYNOLDS" ,
/ 6X,"DYNAMIC" / " ANGLE",6X
/"jib",5X,"main",4X,"VELOCITY",6X,"NUMBER",8X,"FORCE" / )
DO 1 K = 1,NR
WRITE (1,20) ANGLE(K),BJ(K),BM(K),V(K),RE(K),QA(K)
20 FORMAT (1X,F5.1,5X,F5.1,3X,F5.1,4X,F7.2,7X,F8.0,5X,F7.2)
1 CONTINUE

```

C

```

WRITE (1,25)
25 FORMAT (/// " FORCE AND MOMENT COEFFICIENTS RELATIVE TO THE
/"WIND" // " COURSE" / " ANGLE",6X,"Cx",7X,"Cy",7X,"Cz",7X,
/"Cmx",6X,"Cmy",6X,"Cmz" / )
DO 2 K = 1,NR
WRITE (1,30) ANGLE(K),CX(K),CY(K),CZ(K),CMX(K),CMY(K),CMZ(K)
30 FORMAT (1X,F5.1,6(2X,F7.4))
2 CONTINUE

```

C

```

WRITE (1,35)
35 FORMAT (/// " FORCE AND MOMENT COEFFICIENTS RELATIVE TO THE
/"BOAT" // 37X,"HEEL",5X,"PITCH",5X,"YAW" )
WRITE (1,40)
40 FORMAT (" COURSE",4X,"DRIVE",4X,"HEEL",5X,"VERT.",
/ 3(3X,"MOMENT") / " ANGLE",1X,6(4X,"COEF.") / )
DO 3 K = 1,NR
WRITE (1,30) ANGLE(K),CDR(K),CHL(K),CVT(K),CMHL(K),CMPT(K),
/ CMYW(K)
3 CONTINUE

```

C

C

```

RETURN
END

```

APPENDIX E BOUNDARY LAYER SYNTHESIS

Local velocity impedance was achieved by positioning a partial wire cloth upstream of the test section and weaving 4-ply acrylic yarn (0.06 in. diameter) at various heights until the desired profile was developed. An initial experiment was conducted to measure the impedance of the screen/frame assembly alone. With the wind tunnel running at $U(30) = 85$ ft/s, the profile was surveyed both vertically and horizontally. The horizontal survey was used to ascertain lateral uniformity. A second similar experiment was then made with 0.06 in. diameter yarns woven at arbitrary locations parallel to the tunnel floor. A correlation was made between these two experiments for Δz yarn spacing and the resultant change in velocity

$$\Delta \left[\frac{U(z)}{U_{30}} \right] = \left[\frac{U(z)}{U_{30}} \right]_{\text{screen}} - \left[\frac{U(z)}{U_{30}} \right]_{\text{screen/yarn}}$$

measured in the test section (TS-0) for the same relative height (Figure E-1). From the initial arbitrary yarn placement and experiments mentioned above, a discrete empirical predictor was developed by least square curve fit to the formula.

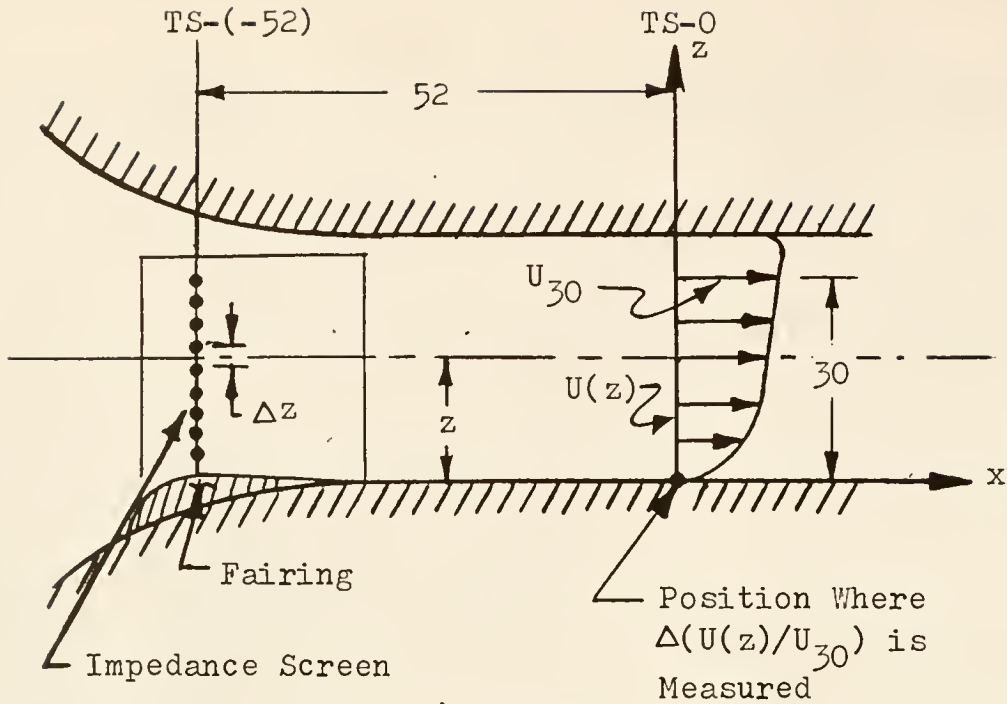


FIGURE E-1 Correlation of Screen Impedance With The Resultant Downstream Velocity Distribution.

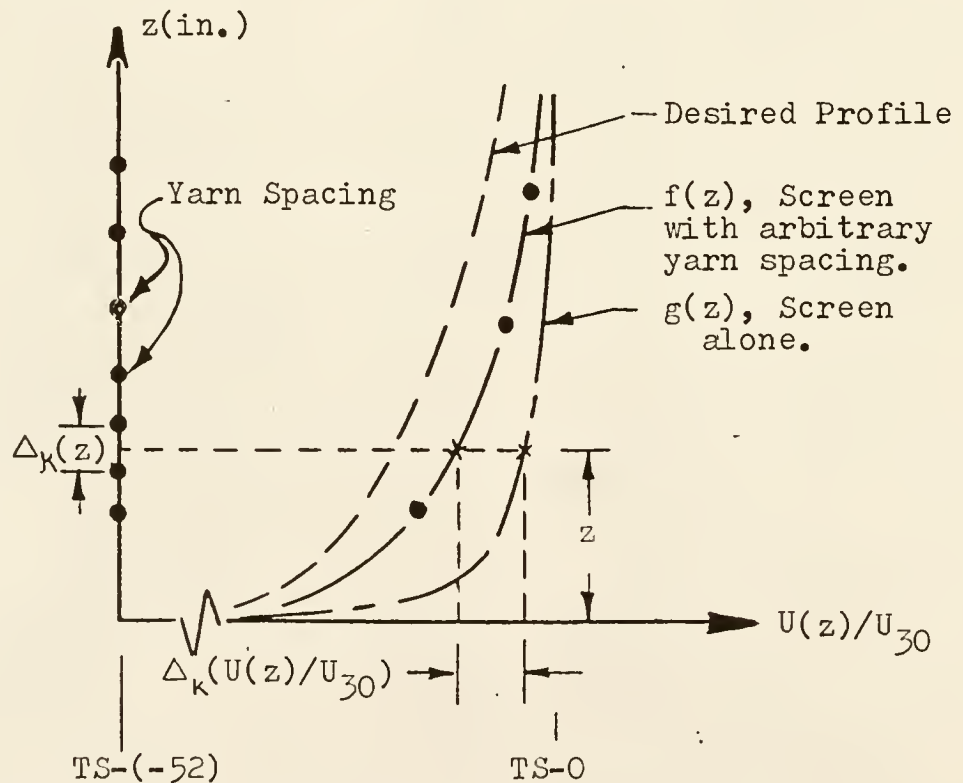


FIGURE E-2 Yarn Spacing And The Resultant Flow Impedance Distribution.

$$\Delta_k(z) = ae^{b\Delta_k \left[\frac{U(z)}{U_{30}} \right]} \quad (E-1)$$

where "a" and "b" are constants determined by the fitting procedure. Figure E-2 illustrates the effect of yarn spacing on the velocity profile which data are fed into the predictor algorithm. With the desired profile known a priori the determination of the required solidity distribution for the required screen can be predicted from the empirical algorithm.

For practical reasons, the screen was placed in the tunnel between 45 in. and 55 in. upstream of the test section zero position. This allowed easy removal of the one-piece screen assembly when the wind tunnel was required for other test purposes. For sailing yacht wind tunnel testing the vertical velocity distribution, $U(z)$, relative to the model is considered to be of principal importance and relative scale of turbulence to be of a lesser concern. As such, the initial screen size selected as as: $1/4 \times 1/4 \times 0.025$ in. wire cloth set 52 in. upstream of the test section zero position (TS-0). The effect on local turbulence at TS-0 was therefore minimal and the local turbulence was thought to be an exclusive function of the yarns. The scale of the turbulence thus generated was determined as indicated in Chapter Two to be at least an order of magnitude larger than that anticipated in the sail model turbulent shear layer and, as such, did not effect the drag properties of this shear layer.

Final form of the velocity profile could then be synthesized using the difference between the velocity distribution caused by the screen alone and the desired velocity distribution from Chapter Five given by equation 5-5. The screen velocity profile was piece-wise curve fitted to a correlation coefficient of 0.99 minimum. This profile may be described as

$$g_i(z) = \begin{cases} g_1(z), & 0 \leq z < a_1 \\ \vdots & \vdots \\ g_n(z), & a_{n-1} \leq z \leq a_n \end{cases}$$

where $i = 1, 2, 3, \dots, n$

and $g_n(z)$ is an admissible function meeting the correlation requirement over $(a_{n-1}) \leq z \leq a_n$. The required profile is known from equation 5-5 and may be written as

$$f(z) = \frac{U(z)}{U_{30}} = A \ln(z) + B$$

where $A = 0.1086$, $B = 0.4918$, $z(\text{in.})$ is the vertical height above the tunnel floor, U_{30} is the velocity at $z = 30$ in., and $U(z)$ is the velocity at any z . The required local k th impedance is then a function of the velocity difference at z_k

$$\Delta_k \left[\frac{U(z)}{U_{30}} \right] = g_i(z_k) - f(z_k)$$

in which form equation E-1 becomes

$$\Delta_k(z) = a e^{b[g_i(z_k) - f(z_k)]}$$

Taking $\Delta_1(z) = a$, initiates the marching scheme. Successive values of the independent variable were determined by the regression from

$$z_{k+1} = z_k + \Delta_k(z)$$

In an effort to ensure smoothness of profile in the vicinity of the piece-wise continuity points (g_i 's) the marching was slightly over extended. Any significant differences in spacing prediction in these areas were then averaged.

The above technique is an excellent way of determining a solidity distribution for a pre-defined velocity profile. In application it has been found to be satisfactory. Only slight modifications were necessary in the final yarn spacing near the top and bottom of the wire cloth screen. Figure E-3 shows the resultant profiles obtained at two different tunnel speeds. The average confidence of fit was 96.5% using a least squares logarithmic curve fit for each. A vertical turbulence survey of the profile appears reasonable and is also shown in Figure E-3.

This turbulence is measured using a 5.0 inch diameter sphere by the method of Dryden and Kuethe (31). The sphere

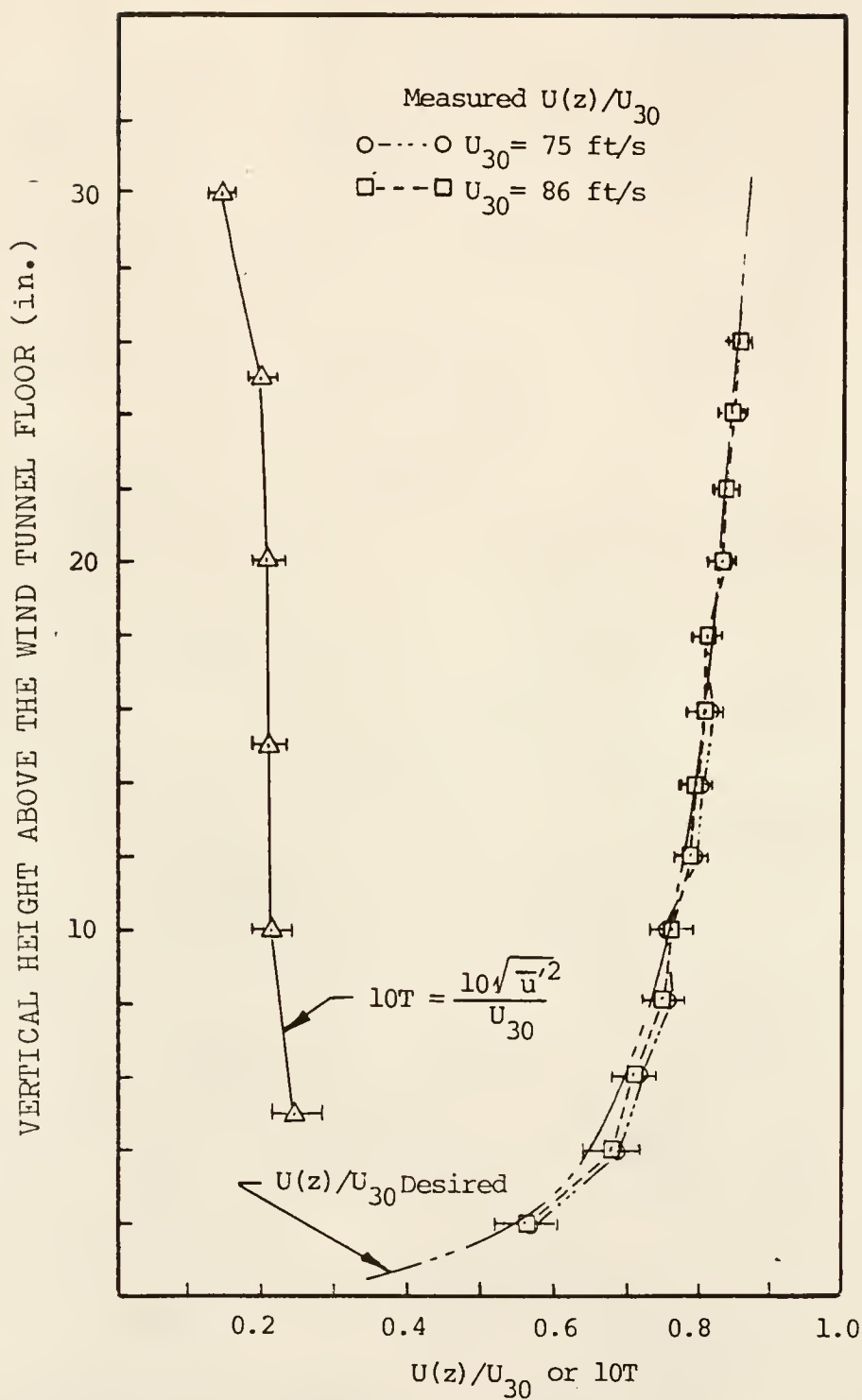


FIGURE E-3 Dimensionless Velocity Ratios (Measured and Desired) and Turbulence Intensity Distribution ($10T$).

is modified to read the pressure difference from the front to the rear of the sphere. Pressure readings are converted to electrical signals through two Validyne P300 D/A pressure transducers and are connected directly to a HP7046A x-y plotter. By varying the tunnel speed from low to high, a direct plot of pressure difference on the sphere versus dynamic pressure was obtained. The critical drag point indicating transition to a turbulent boundary layer was taken as

$$\Delta P = 1.22q$$

The sphere's Reynolds number at this point was an indication of the stream local turbulence intensity. Positioning the sphere at 5.0 in. vertical increments leads to the plot shown. The bands at each datum point on the curve indicate the uncertainty of the reading. Comparing Figure E-3 with the classical turbulent boundary layer for flow over a flat plate shows reasonable agreement.

APPENDIX F
CENTER OF EFFORT BY DIRECT INTEGRATION

As discussed in Chapter Eight, the concentration of points around the planform centroid suggests an analytical check using a direct integration scheme for both cases of the Finn sail tested; with and without a simulated PBL. The following analysis is presented in support of the conclusions made in Chapter Eight.

For the Finn sail planform, define the force center vertical distance as

$$\bar{z} = \frac{\int_0^H z \, dL}{\int_0^H dL}$$

where z equals the distance from the wind tunnel floor, dL is the differential lift force component in the horizontal plane at z .

Then let

$$dL = q(z) C_L(z) C(z) dz$$

where

$q(z)$ equals the local dynamic pressure,

$C_L(z)$ is the local section lift coefficient, and

$C(z)$ is the chord length at z

Then

$$\bar{z} = \frac{\int_0^H z Q(z) C_L(z) C(z) dz}{\int_0^H Q(z) C_L(z) C(z) dz} \quad (F-1)$$

The local length of chord is adequately represented by the linear model of the form

$$C(z) = K_1 z + K_2$$

where K_1 and K_2 are constants determined from the sail planform. For the uniform velocity, dynamic pressure is independent of z . Therefore,

$$\bar{z} = \frac{\int_0^H z C_L(z) (K_1 z + K_2) dz}{\int_0^H C_L(z) (K_1 z + K_2) dz} \quad (F-2)$$

It is now a matter of selecting a sensible relationship for lift coefficient. As a first approximation C_L can be considered a constant and independent of z . This is comparable to assuming each accurate section of the sail has the same lift characteristics at any one given angle of attack. Further, this approximation infers that the sail twist is matched to the vertical apparent wind twist such that the angle of attack is independent of z . In this approximation, equation F-2 identically yields the planform area centroid height of $\bar{z} = 0.394$ in. This value is plotted in Figure 8-5 and is seen to be in good agreement with the measurements

made for attack angles from 25° to 35°. Recalling the 0.36 H vertical height noted in the tuft observation experiment, it appears that the windward/leeward centers of pressure are most likely different, the latter being well above the former, and resolve to 0.39 H.

For the nonuniform onset velocity, equation F-1 with uniform flow density reduces to

$$\bar{z} = \frac{\int_0^H z (A \ln z + B)^2 (K_1 z + K_2) C_L(z) dz}{\int_0^H (A \ln z + B)^2 (K_1 z + K_2) C_L(z) dz} \quad (F-3)$$

where A and B are constants of the onset velocity distribution. It is now necessary to select an appropriate functional form for the spanwise lift coefficient.

As a first approximation this function is assumed proportional to the actual sail twist. This leads to a transcendental cosine function which overly complicates the integration of equation F-3. Approximating this cosine function as a polynomial allows equation F-3 to be integrated by parts, the zeroth degree polynomial being identical to a constant coefficient of lift as in the uniform flow case. For interest, both first and second degree approximating polynomials are calculated and applied to equation F-3 with the following results:

<u>Approximating Polynomial Degree</u>	<u>Height to Force Center, z (in)</u>	<u>z/H</u>
0	13.35	0.48
1	13.55	0.48
2	13.56	0.48

Comparing these theoretical values to the measured values given in Figure 8-5 shows reasonable agreement and adds credibility to the measured values.

In order to verify and check the overall procedure and apparatus, the magnitude of the x, y, z forces were calculated for the 30° angle of attack force coefficient determined by test with the simulated PBL. Using an adjustable stanchion in place of the sail model, dead loads of the same magnitude ($\pm 1.0\%$) as those calculated for the 30° angle of attack were statically applied at the estimated force center indicated in Figure 8-5. The resistances thus produced in the six force balance strain guage channels agreed with the original dynamic strain readings obtained during the wind tunnel test to within $\pm 8\%$. While large, these values of uncertainty are considered reasonable because of the duplication of uncertainty through the calibration coefficients. The calculations made here add further credibility to the correctness of the estimated force center.

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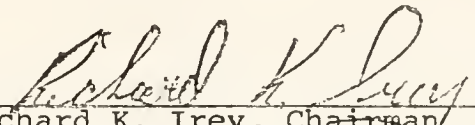
BIOGRAPHICAL SKETCH

James G. Ladesic was born July 10, 1946, in Pittsburgh, Pennsylvania. He attended Hampton High School In Allison Park, Pennsylvania and graduated June 1964. His undergraduate studies were completed August 1967, in aeronautical engineering at Embry-Riddle Aeronautical University (ERAU), Daytona Beach, Florida. After spending four years in the aerospace industry in mechanical design and research/development activities, Jim entered graduate school at the Florida Technological University in Orlando (now the University of Central Florida). A Master of Engineering degree was conferred August 1973 in engineering mechanics. During graduate studies Jim began teaching undergraduate engineering mathematics at ERAU and remained in academia until June 1978. During this same period, practically all of his free time was devoted to the design, fabrication, and development of ultralight aircraft and to sailing research.

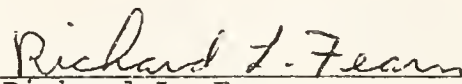
Jim began graduate studies toward a Ph.D. in mechanical engineering at the University of Florida in 1976 and continued through 1977. This effort was interrupted by a four-year return to engineering practice where he functioned in design, development, and mechanical vibration analysis. In 1981, he rejoined ERAU's faculty as an Associate Professor of Engineering and continued his pursuit of the Ph.D.

Upon graduation, Jim wishes to continue working in both the academic and industrial environments. He is a licensed engineer in the State of Florida and is president of Associated Engineering Technologies, Inc., a small but hopeful firm consulting in the applied engineering sciences.

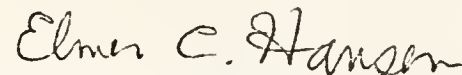
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Richard K. Trey, Chairman
Professor of Mechanical Engineering

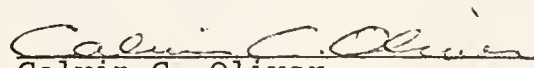
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Richard L. Fearn
Professor of Engineering Sciences

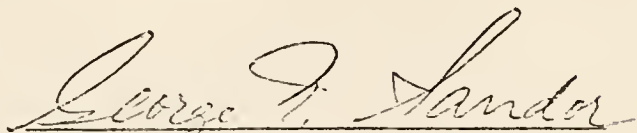
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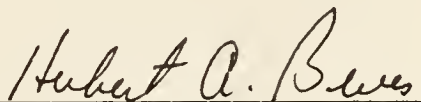
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This dissertation was submitted to the Graduate Faculty of the College of Engineering and to the Graduate Council, and was accepted as partial fulfillment of the requirements for the degree of Doctor of Philosophy.

January, 1983



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